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The Compressive Strength of Thin Aluminum Alloy Sheet in the Plastic Region

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Introduction

During the past few years the trend of design in the aircraft industry has been toward the use of the high-strength aluminum alloys. Designers have found that more efficient structures could be fabricated by using the high strength of the aluminum alloys such as 24ST extruded dural, 24ST alclad, and 24SRT alclad, hence, these alloys, having a strength to weight ratio equal to or better than most other structural materials, have found wide application in the design of all types of airplanes. Due, however, to the rapid increase in speeds and the necessity for higher pay loads, designers and engineers must now analyze structures more accurately than in the past, thereby introducing a new structural problem which involves the behavior of the aluminum alloys at high stresses.

The behavior of the aluminum alloys when subjected to stresses beyond their proportional limit differs from that of many familiar ferrous alloys in that their stress-strain relationships only obey Hooke's law up to a relatively low stress, and then deviate so that Young's modulus (E) is no longer a constant. This has a decided effect on the computation of stresses in a structure since most structural equations for the determination of stresses are based on the assumption that E is constant. W. L. Howland[1] brought this to the attention of aircraft designers some time ago and showed the variation of the modulus with stress in tension at the high stresses, tensile stresses being used instead of compressive stresses due to the lack of an adequate method for determining compressive stresses at the time. However, compressive stresses are usually more critical in aircraft structures than tensile stresses and consequently it is more important to understand fully the behavior of the aluminum alloys in compression. Design formulas must now predict the actual stresses in the structure more accurately than before, since design compressive stresses are rapidly approaching the ultimate strengths of the materials, and since structures are frequently composed of several types of material having different stress-strain relationships in the plastic region.

While Howland's work is not strictly applicable to formulas for predicting the stresses in structural members subjected to compression because of the variation between tensile and compressive properties of the aluminum alloys, it does indicate that formulas based on material stress-strain relationships must be corrected when the stresses no longer follow Hooke's law. The investigation presented here deals with compressive stresses and, since little information is available in technical literature concerning the compressive properties of the aluminum alloys, this subject will be covered in the light of the test data available at Lockheed.

Compressive Properties of Aluminum Alloys

Before discussing the determination and the effect of the modulus at high stresses, it is first necessary to examine the compressive stress-strain curves of the alloys concerned. In Figs. 1 and 2 are shown some typical compression stress-strain curves for small 24ST extrusions, 24ST alclad sheet (10%), and 24SRT alclad sheet (10%) for directions of load both parallel and perpendicular to the direction of rolling of the sheet for the latter two materials in the "as received" condition. These curves represent the average of compression tests on several specimens of different thicknesses and it is reasonable to assume that the curves are typical for the particular materials tested since the compression specimens were selected from stock at random. The compressive stresses were determined from tests on thin strips of the material supported laterally to prevent buckling and by using Huggenberger extensometers to record the compressive strains. A similar method was

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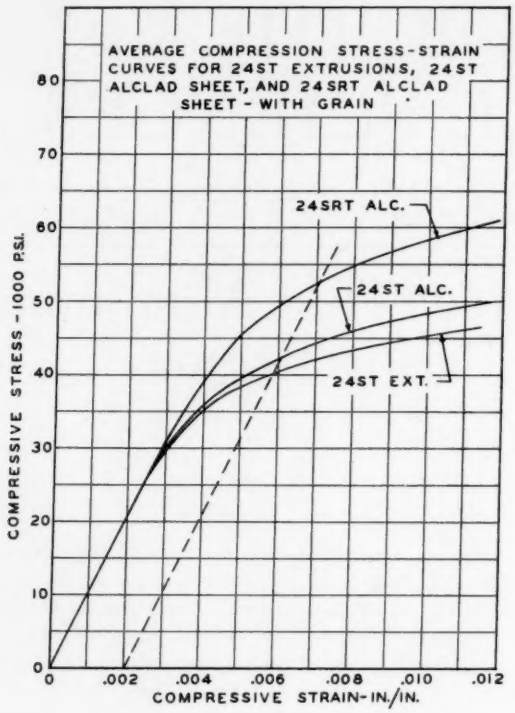

FIG. 1.

recently reported by the National Bureau of Standards.[2] In Fig. 3 are plotted some of the individual

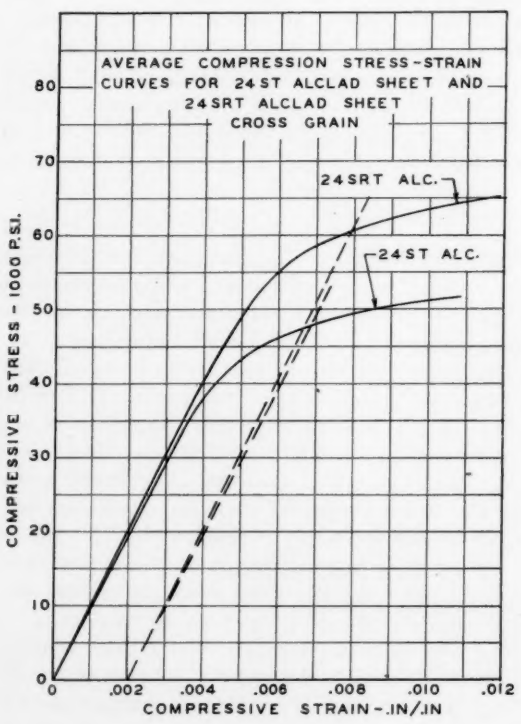

FIG. 2.

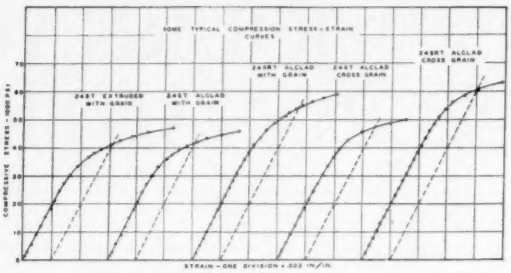
SOME TYPICAL COMPRESSION STRESS-STRAIN CURVES
24ST EXTRUDED WITH GRAIN
24ST ALCLAD WITH GRAIN
24SRT ALCLAD WITH GRAIN
24ST ALCLAD CROSS GRAIN
24SRT ALCLAD CROSS GRAIN

FIG. 3.

tests with the type of material and grain direction noted and in Tables 1, 2 and 3 typical relations between the tensile and compressive yield stresses are given, the moduli in the tables being the initial moduli. The accuracy of the extensometers used for measuring the compressive stresses was not sufficient to detect the effect on the initial moduli of yielding of the aluminum coating at low stresses. However, any discrepancy resulting from this will not affect the results presented here as this investigation is primarily concerned with the stresses in the plastic region.

An examination of the figures and tables mentioned above readily indicates the large variations between the tensile and compressive yield stresses, and between with-grain and cross-grain yield stresses. With-grain compressive stresses are considerably below the with-grain tensile stresses while the cross-grain compressive stresses are higher than the cross-grain tensile stresses for the alclad sheet. These variations are not significant for design purposes until design stresses approach the yield stresses (defined by the 0.2 per cent offset method), then these variations must be given due consideration. The grain direction must be identified with the given stress and the type of stress must also be stated (whether tension or compression).

The relations between the tensile and compressive properties given in Tables 1, 2 and 3 supplement those reported by the Aluminum Company of America in Bulletin 325-M,[3] and are in close agreement with them. The use of these relations should be obvious. Frequently it is desired to obtain yield stresses in a direction other than that given by test data as frequently it is desired to know the value of a compressive yield stress when the tensile yield stress is given. These may be found by use of the relations given in Tables 1, 2 and 3. As an example of this, let it be assumed that the cross-grain tensile yield stress for 24ST alclad sheet is given as 43,000 psi. and that the corresponding with-grain compressive yield stress is desired. From the relations given in Table 2 the compressive yield stress (w) = 0.97 tensile yield (x) or $0.97 \times 43{,}000 = 41{,}700$ psi. It should be noted here that these relations should only be used for those yield stresses for which they were determined, namely, the yield stresses defined by the 0.2 per cent offset method. Since the stress-strain curves for tension and compression are not affinely

TABLE 1

Tests on 24ST Extruded Material

Specimen No.	Grain Direction	Material Thickness	Compressive Yield Stress psi.	Compressive Modulus $\times 10^6$ psi.	Tensile Yield psi.	Tensile Modulus $\times 10^6 \times$ psi.
A	With	0.0665	41,600	10.1	49,500	11.4
B	With	0.0665	41,600	10.2	48,900	10.7
A	With	0.092	39,000	10.4	48,500	11.1
B	With	0.092	38,800	10.6	48,500	10.9
Average			40,300	10.3	48,900	11.0

TABLE 2

Tests on 24ST Alclad Sheet (10%)

Specimen No.	Grain Direction	Material Thickness	Compressive Yield Stress psi.	Compressive Modulus $\times 10^6$ psi.	Tensile Yield psi.	Tensile Modulus $\times 10^6$ psi.
A	With	0.040	43,600	10.0	50,000	10.2
B	With	0.040	43,100	10.3	49,800	10.0
A	With	0.064	42,100	9.7	51,000	10.2
B	With	0.064	41,500	9.7	50,600	10.0
Average	With		42,600	9.9	50,400	10.1
A	Cross	0.040	47,400	10.1	44,600	10.5
B	Cross	0.040	47,100	10.1	44,200	10.8
A	Cross	0.064	47,400	9.7	42,800	10.2
B	Cross	0.064	48,700	9.7	44,000	9.6
Average	Cross		47,700	9.9	43,900	10.3

Average tensile yield (w) = *1.18* average compressive yield (w).
Average tensile yield (X) = *0.92* average compressive yield (X).
Average compressive yield (w) = *0.97* average tensile yield (X).

TABLE 3

Tests on 24SRT Alclad Sheet (10%)

Specimen No.	Grain Direction	Material Thickness	Compressive Yield Stress psi.	Compressive Modulus $\times 10^6$ psi.	Tensile Yield psi.	Tensile Modulus $\times 10^6$ psi.
A	With	0.040	54,100	10.2	62,900	9.9
B	With	0.040	52,800	10.2	62,400	10.0
Average	With		53,500	10.2	62,700	10.0
A	Cross	0.040	61,000	10.3	55,900	9.8
B	Cross	0.040	60,500	10.1	56,000	10.2
Average	Cross		60,800	10.2	56,000	10.0

Average tensile yield (w) = *1.17* average compressive yield (w).
Average tensile yield (x) = *0.92* average compressive yield (x).
Average compressive yield (w) = *0.96* average tensile yield (x).

related, these relations will not necessarily apply to any other defined yield stresses.

It might be well to point out here that the yield stresses, conversion factors, and stress-strain curves are typical or average test values for material in the "as received" condition and should not be regarded at absolute or minimum values. They are subject to certain variations since they are affected by the cold working process to which the sheet is subjected in order so obtain its flatness. Accurate control cannot be maintained over the strength of the sheet due to this process and consequently the values of stress given here must be regarded only as typical or average values and any comparisons between analyses based on these values and test data may be subject to a certain amount of discrepancy due to these material variations.

SYMBOLS

The symbols used throughout this investigations are presented below.

E = compressive modulus of elasticity in psi.
E_s = compressive secant modulus of stiffener material in psi.
E_s' = compressive secant modulus of sheet material in psi.
E_t = compressive tangent modulus of sheet material in psi.

E_r = reduced compressive modulus of sheet material in psi.
b = stiffener spacing in inches
b_e = effective width in inches
t = sheet thickness in inches
R = radius of curvature in inches
e = strain in in./in.
f = stress in psi.
K, C = constants

Variation of Young's Modulus of Elasticity

The modulus of elasticity defines the rigidity of a material and is determined from the ratio of stress to strain for the material. This modulus is used in all structural work and is usually assumed to be constant. However, an examination of the stresses in Fig. 3 shows that the ratio of stress to strain for the aluminum alloys in compression is not constant beyond stresses of approximately 20,000 psi. and the problem that arises from this is the determination of a modulus that, when used in structural formulas, will predict the behavior of structures above this stress.

Except for the constant modulus the tangent modulus is, perhaps, the most commonly used modulus at stresses beyond the proportional limit and is defined as the ratio df/de or the rate of change of stress with strain. It is a measure of the slope of the stress-strain diagram at any point and was first suggested by Engesser.[4] It has been suggested for use in most stability equations but sufficient experimental verification of its use is still lacking. In the Aluminum Company Technical Paper No. 1[5] the tangent modulus was compared with test data on some extruded aluminum alloy tubes and good agreement was obtained. However, this represents a special case and other test data, as yet, do not amply check this agreement. (For tangent modulus see Fig. 4.)

Another form of the modulus known as the reduced or double modulus has been suggested by von Kármán and is based on a combination of the tangent modulus E_t, the constant modulus E, and the shape of the cross-section of the compression member. When bowing or buckling of a member in compression occurs, tensile stresses are introduced on one side and compressive stresses on the opposite side. This corresponds to the stresses in an ordinary beam under bending in which tensile and compressive stresses are set up on opposite sides of the neutral axis as shown in Fig. 5. The compressive stresses follow the type of stress-strain curve shown in Fig. 1 while the tensile stresses obey a linear law as indicated by the return curve in Fig. 6. This condition exists when instability of a compression member takes place such as in the case of an Euler column or the initial buckling of flat or curved sheet. For rectangular cross-sections such as flat sheet the theoretical equation for the reduced modulus is

$$E_r = \frac{4EE_t}{(\sqrt{E} + \sqrt{E_t})^2} \qquad (1)$$

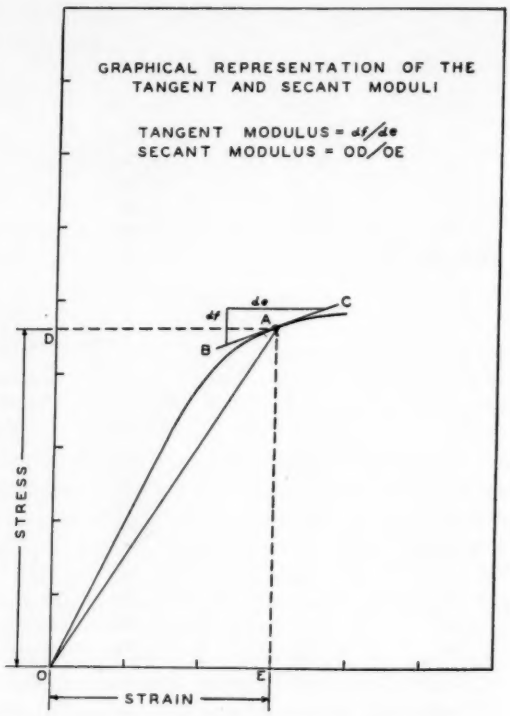

Fig. 4.

where E is the constant modulus representing the tensile stresses and E_t is the tangent modulus representing the compressive stresses. The development and further explanation of this modulus will be found in reference 6. For predicting initial instability this modulus is theoretically more correct than the tangent modulus.

Another modulus that appears less frequently in technical literature is known as the secant modulus which is defined as the slope of a line from the origin to any point on the stress-strain diagram. It is a measure of the deformation of the material at any stress and may be determined from the relation f/e where e is the total strain at the material stress f. In Fig. 4 the method for determining the secant modulus is shown. At point A on the stress-strain curve the secant modulus is the ratio of the total stress f to the total strain e, or in other words the slope of the line OA. The tangent modulus at point A is represented by the slope of the line BC or df/de. It may be seen from this figure that at low stresses both the secant and tangent moduli

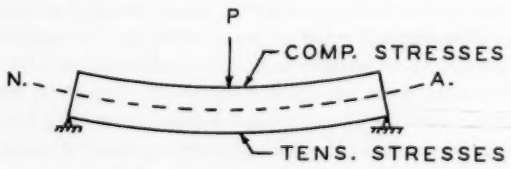

Fig. 5. Simple beam under bending showing type of stresses developed.

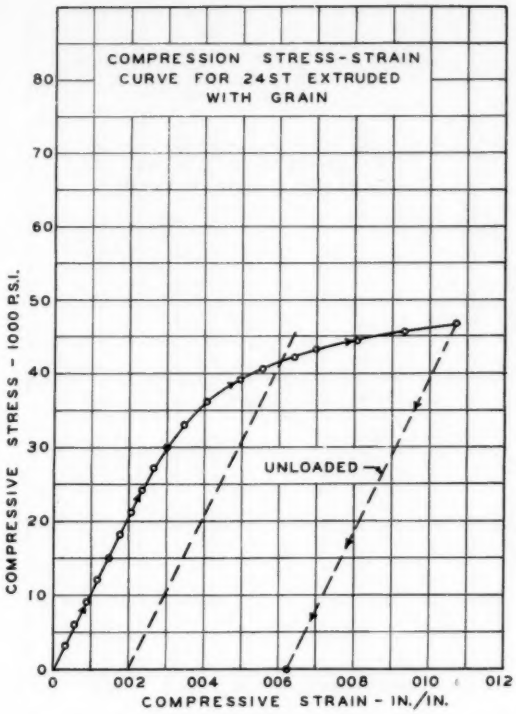

FIG. 6. Compression stress-strain curve for small 24ST extruded section showing type of return curve when section is unloaded.

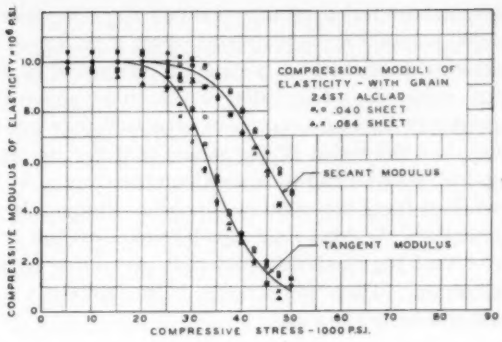

FIG. 9.

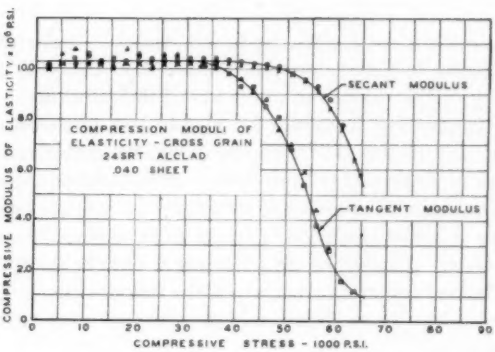

FIG. 10.

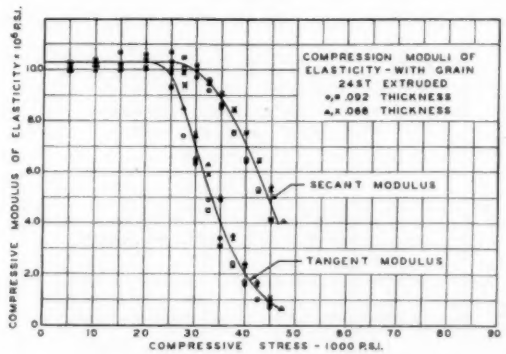

FIG. 7.

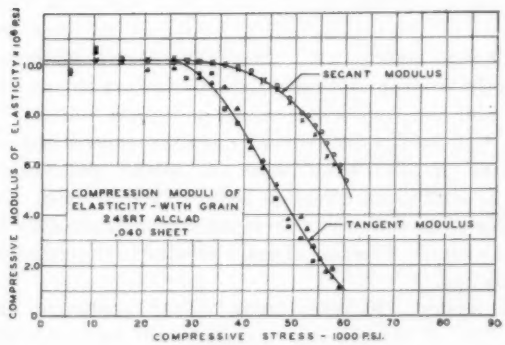

FIG. 11.

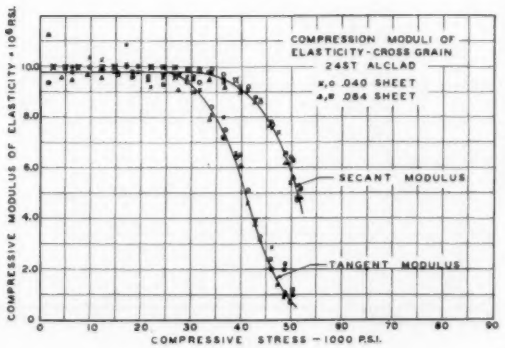

FIG. 8.

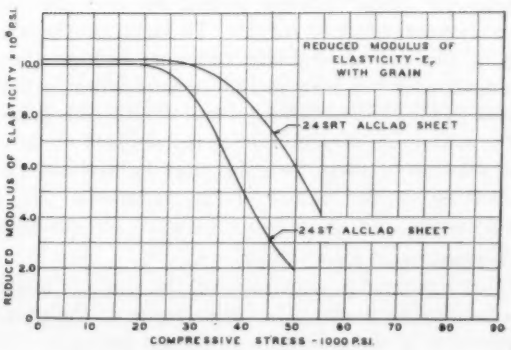
REDUCED MODULUS OF ELASTICITY - E_r WITH GRAIN
24SRT ALCLAD SHEET
24ST ALCLAD SHEET
REDUCED MODULUS OF ELASTICITY x 10⁶ P.S.I.
COMPRESSIVE STRESS - 1000 P.S.I.

FIG. 12.

correspond to the constant modulus E. The secant modulus is useful at high stresses where it is desired to represent the strength of a structure composed of dissimilar materials. The value of the stress above the proportional limit in a composite structure may not be known, but the value of the strain or deflection at a given load may be known from which the stress may be determined from the relation $f = eE_s$. Frequently at high stresses the load may also be unknown but it is known that the dissimilar materials are acting at the same strain. Then the stresses in the different materials will be in the ratio

$$\frac{f_1}{f_2} = \frac{E_{s1}}{E_{s2}} \tag{2}$$

This relation will often be found useful in structural equations to determine the relative load carrying capacity of the various components of the structure. In this connection it should be noted that analyses for structures in the plastic region should be considered in terms of strain rather than stress. Structural equations for the strength analyses of structures are usually based on an assumed condition of strain and not stress, hence, the failure strength of the structure should be analyzed on the basis of strain from which the stresses may be determined by a consideration of the material stress-strain relationships.

For use in predicting the compressive strength of flat or curved sheet elements in a structure, curves of tangent and secant moduli have been plotted in Figs. 7 to 11 for 24ST alclad sheet, 24SRT alclad sheet and small 24ST extrusions. These curves were computed directly from a number of compression tests on the material and represent average or typical values. In Fig. 12 curves of the reduced modulus *versus* stress are plotted for 24ST alclad sheet and 24SRT alclad sheet in the with-grain direction only. This direction was chosen as all stiffener or sheet material will normally be used with the direction of grain parallel to the direction of loading.

In many structural formulas for predicting the stability of compression members will be found the dimensionless parameter f/E such as in the familiar Euler column formula where, $f/E = C\pi^2\rho^2/L^2$, or in the formula for initial buckling of thin flat sheet in compression where, $f/E = Kt^2/b^2$. To facilitate the solution of the various stability formulas such as these, this parameter has been plotted against stress in Figs. 13 and 14 for 24ST alclad sheet and 24SRT alclad sheet in the with-grain direction using the reduced modulus. The solution of these formulas may be accomplished by setting various values of the parameter f/E_r in the formulas and transposing to solve for the other parameter, *i.e.*, $t/b = \sqrt{(f/E_r)/K}$.

In the foregoing discussion of the various moduli of elasticity no mention has been made of the proper application of the moduli to structural formulas. Use of the tangent modulus has not, as yet, been adequately

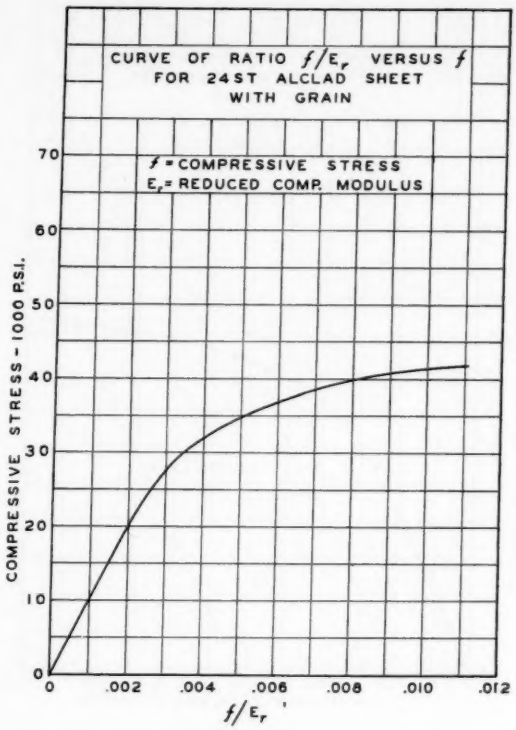

FIG. 13.

justified and, although the reduced modulus has been shown to be theoretically more correct than the tangent

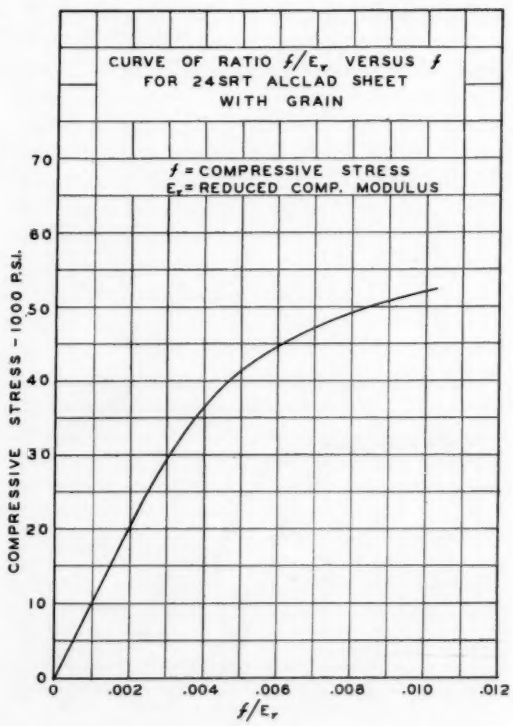

FIG. 14.

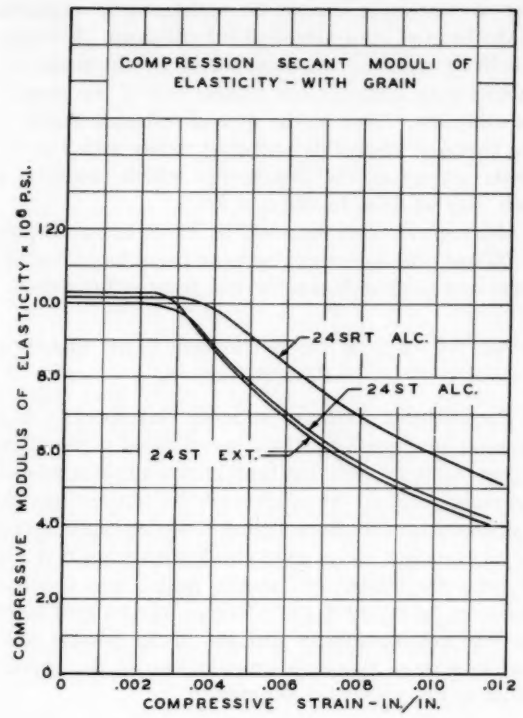

FIG. 15.

modulus in stability formulas, justification for its use is still lacking. However, just as justification for the use of any variable, parameter, or equation depends upon the accuracy with which it predicts experimental data in all cases, so justification for the use of any modulus will depend upon the accuracy with which it predicts the behavior of a structure under load. In the following section the secant and reduced moduli are used in structural equations and a comparison with test data is made to justify the use of these moduli in equations for predicting the strength of flat and curved sheet in compression. The reduced modulus is used in preference to the tangent modulus because as previously explained it is theoretically more correct for rectangular sections than the tangent modulus for stability equations.

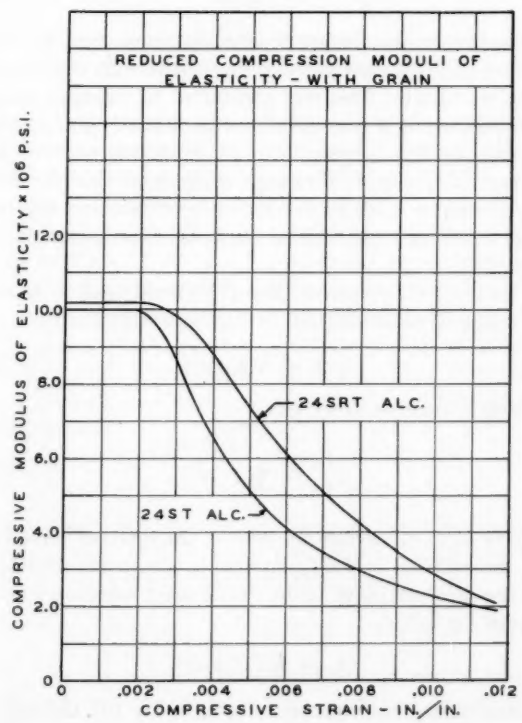

FIG. 16.

INITIAL BUCKLING OF STIFFENED FLAT SHEET IN COMPRESSION

Flat, stiffened, sheet-stringer combinations, as used in semimonocoque construction, offer a simple example for a comparison of the use of the various moduli with test data. Initial buckling of the sheet between stringers of a stiffened panel due to compression being a function of the modulus of elasticity and the dimensions of the panel may be expressed by the equation[7]

$$f_{cr} = KEt^2/b^2. \tag{3}$$

This equation is valid in the elastic (constant modulus) region only. However, since this equation is in the form of a stability equation, it may be used in the plastic region by substituting the reduced modulus for the constant modulus. Substituting and transposing, the equation then becomes

$$b/t = \sqrt{KE_r/f_{cr}} \tag{4}$$

E_r and f_{cr} correspond to the sheet material of the panel but in a composite structure such as a stiffened panel where 24SRT alclad stringers are attached to 24ST alclad sheet, the given or measured stresses are usually those of the stiffener. Hence, in the plastic region where the two material stress-strain curves differ, a correction must be made to account for this difference. Knowing that initial buckling occurs at a certain unit strain corresponding to the sheet buckling stress (f_{cr}), the stiffener stress (f_{st}) at which initial buckling of the sheet will occur will be, from Eq. (2),

$$f_{cr} = f_{st} \times E_s'/E_s \tag{5}$$

where E_s corresponds to the stiffener and E_s' corresponds to the sheet. Substituting this in Eq. (4)

$$b/t = \sqrt{\frac{KE_rE_s}{f_{st}E_s'}} \tag{6}$$

For the solution of this type of equation, curves of the secant modulus and reduced modulus have been plotted against strain in Figs. 15 and 16 and from these figures and Fig. 1 the various values of f_{st}, E_r, E_s and E_s' corresponding to the proper material, and taken at the

same unit strain, may be substituted in Eq. (6) to obtain values of b/t. It should be noted that, since f_{st} and E_r in the above equation correspond to different materials, the values for f/E_r in either Figs. 13 or 14 should not be used in this equation.

The constant K in Eq. (6) has been computed by Timoshenko[7] for various types of edge restraint. In the work presented here only two types of restraint are considered, namely, simple support which allows rotation but no translation of the edges and clamped or fixed support which allows neither translation nor rotation of the edges. For simple support the minimum value of K is given as 3.62 and for fixed support the value is given as 6.32.

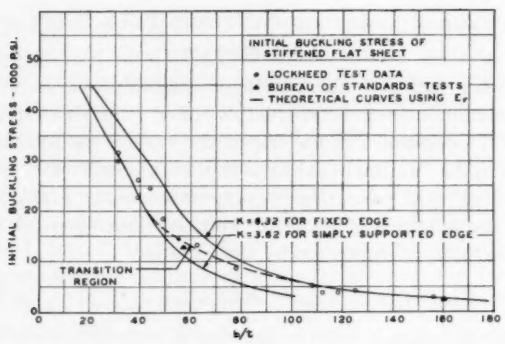
INITIAL BUCKLING STRESS OF STIFFENED FLAT SHEET
• LOCKHEED TEST DATA
▲ BUREAU OF STANDARDS TESTS
— THEORETICAL CURVES USING E_r
INITIAL BUCKLING STRESS - 1000 PSI.
K=6.32 FOR FIXED EDGE
K=3.62 FOR SIMPLY SUPPORTED EDGE
TRANSITION REGION
b/t

FIG. 17. Comparison of theoretical initial buckling stresses for 24ST alclad sheet with test data (all stresses with-grain).

In Fig. 17 theoretical curves of initial buckling stress for 24ST alclad sheet have been plotted against b/t for the two types of support using the reduced modulus in the plastic region. Initial buckling data from Lockheed stiffened panel tests as well as two values of initial buckling stress from N.A.C.A. Technical Note 684 are compared with these curves. The Lockheed test points are averages of several panels and were determined by several methods such as visual observation, direct measurement with dial gages, contour measurement, and effective width measurement. Although the test data include both 24ST alclad and 24SRT alclad stiffeners, the comparison with the curves for 24ST alclad sheet uncorrected for different stiffener materials is valid since none of the stresses exceed the point beyond which the two material stress-strain curves differ appreciably (Fig. 1).

The test data in Fig. 17 agrees with neither curve throughout the entire range of b/t but in the region of high b/t values (above 100) the agreement with the curve for clamped edges is good and in the region of low b/t values (below 40) the agreement with the curve for a simply supported edge is good. However, between these two regions of b/t there appears to be a transition from one type of support to the other. Since the test data presented in Fig. 17 includes corrugation-sheet panel data as well as stringer-sheet panel data, the variation or transition cannot be satisfactorily explained on the basis of the rotation of the stiffeners. However, it is likely that the moments set up by the sheet as it buckles may influence the attachment of the sheet to the stiffeners. That is, the type of restraint offered by the rivets or spotwelds probably varies with the moments set up at the attachment which moments in turn vary as some function of b/t.

Although the test data in Fig. 17 do not go beyond 30,000 psi., the agreement between the reduced modulus curve and the test data up to this point is fairly good.

Effective Width of Stiffened Flat Sheet in Compression

The effective width of stiffened flat sheet in compression has been frequently investigated in the elastic region but little work has been done on this problem in the plastic region. In reference 8 the tangent modulus was substituted for the constant modulus in an effective width equation and a good check with some test data in both the elastic and plastic region was obtained. However, as stated in the reference, the tangent modulus was not adequately justified in the plastic region and so a more thorough investigation of the effective width in this region is necessary.

In the above reference an expression for the effective width of stiffened flat sheet was given for the condition of clamped edges at the stiffeners based on the von Kármán relationship

$$b_e/b = \sqrt{f_{cr}/f_{st}}. \tag{7}$$

However, it was noted in this reference that at low values of b/t (below 75) the agreement with this equation was poor. This was attributed to changing edge conditions but it may also have been partly due to the initial buckling stresses being in the plastic region. In either case, a more thorough analysis of the effective width can be made in this region by expanding Eq. (7) and resorting to the initial buckling data presented in Fig. 17.

To be strictly correct, Eq. (7) should read in terms of strain as recommended by Marguerre.[9] Then

$$b_e/b = \sqrt{e_{cr}/e_{st}} \tag{8}$$

which in turn becomes

$$\frac{b_e}{b} = \sqrt{\frac{f_{cr}E_s}{f_{st}E_s'}} \tag{9}$$

where E_s is the secant modulus of the stiffener material at the stiffener stress f_{st}, and E_s' is the secant modulus of the sheet material at the initial sheet buckling stress. From Eq. (4)

$$f_{cr} = KE_r/(b/t)^2.$$

Substituting this expression for f_{cr} in Eq. (9), the equation for the ratio b_e/b becomes

$$\frac{b_e}{b} = \frac{t}{b}\sqrt{\frac{KE_T}{E_s'}}\sqrt{\frac{E_s}{f_{st}}} = C\frac{t}{b}\sqrt{\frac{E_s}{f_{st}}}$$

or

$$\frac{b_e}{t} = C\sqrt{\frac{E_s}{f_{st}}} \tag{10}$$

where

$$C = \sqrt{\frac{KE_T}{E_s'}} = \frac{b}{t}\sqrt{\frac{f_{cr}}{E_s'}} \tag{11}$$

from Eq. (4). The solution for C has been put in graphical form in Fig. 18 by plotting C against b/t using values of f_{cr} from Fig. 17. In the region of high values of b/t, values of f_{cr} were taken from the curve for clamped edges, in the region of low values of b/t, from the curve for simply supported edges, and in the intermediate or transition region ($40 < b/t < 100$), from the dotted curve. Values of E_s' corresponding to f_{cr} were taken from Fig. 9. Since in Eq. (11) the ratio f_{cr}/E_s' is a measure of strain, the value of C obtained by this method will apply to all types of stiffener material.

To account for the difference between stiffener and sheet stress-strain relationships, Eq. (10) should be corrected as outlined in reference 8. Then

$$\frac{b_e}{t} = C\frac{E_s'}{E_s}\sqrt{\frac{E_s}{f_{st}}} \tag{12}$$

where E_s' and E_s are taken at the same unit strain (see Fig. 15) corresponding to that represented by the ratio E_s/f_{st}.

An effective width curve as represented by Eq. (12) has been computed and plotted in Fig. 18 for 24ST alclad sheet attached to several types of stiffeners by plotting b_e/Ct against f_{st}.

Experimental verification for the use of Fig. 18 is given in Figs. 19, 20 and 21, in which Lockheed test data is compared with the effective width curves in the plastic region of stress as computed from Fig. 18 for several low values of b/t. Good agreement with the test data is obtained up to the highest test stresses. These high stresses were obtained on corrugation-sheet combinations.

Initial Buckling of Curved Sheet

In cylindrical shells under compression the initial buckling stress of the curved sheet has been expressed by W. A. Wenzek[10] by the stability equation

$$f_{cr} = (f_{cr})_{flat} + (f_{cr})_{curved} = 5E(t/b)^2 + 0.3Et/R \tag{13}$$

where $(f_{cr})_{flat}$ is the initial buckling stress of the equivalent flat sheet and $(f_{cr})_{curved}$ is the buckling stress due to curvature. The flat sheet buckling constant is given as 5.0 which differs from those given in

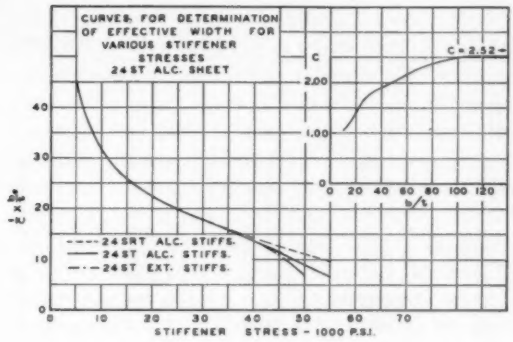
CURVES FOR DETERMINATION OF EFFECTIVE WIDTH FOR VARIOUS STIFFENER STRESSES
24 ST ALC. SHEET
C
C = 2.52
24 SRT ALC. STIFFS.
24 ST ALC. STIFFS.
24 ST EXT. STIFFS.
STIFFENER STRESS - 1000 P.S.I.

FIG. 18.

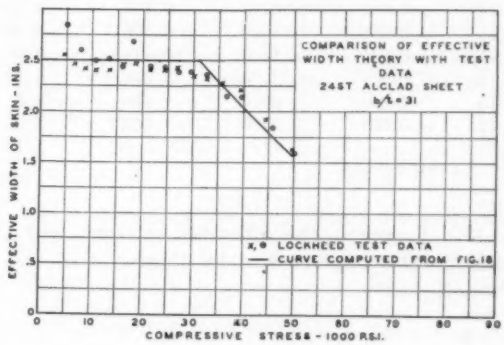
COMPARISON OF EFFECTIVE WIDTH THEORY WITH TEST DATA
24 ST ALCLAD SHEET
b/t = 31
x, ● LOCKHEED TEST DATA
CURVE COMPUTED FROM FIG. 18
EFFECTIVE WIDTH OF SKIN - INS.
COMPRESSIVE STRESS - 1000 P.S.I.

FIG. 19.

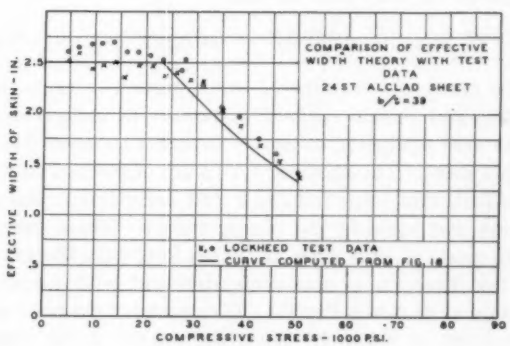
COMPARISON OF EFFECTIVE WIDTH THEORY WITH TEST DATA
24 ST ALCLAD SHEET
b/t = 39
x, ● LOCKHEED TEST DATA
CURVE COMPUTED FROM FIG. 18
EFFECTIVE WIDTH OF SKIN - IN.
COMPRESSIVE STRESS - 1000 P.S.I.

FIG. 20.

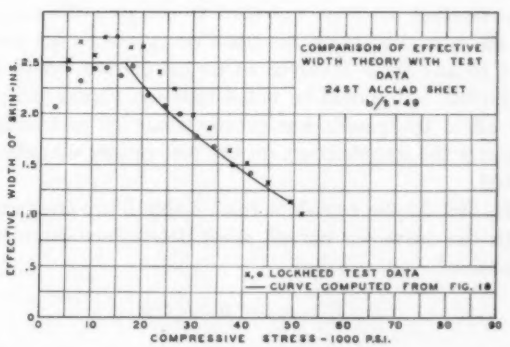
COMPARISON OF EFFECTIVE WIDTH THEORY WITH TEST DATA
24 ST ALCLAD SHEET
b/t = 49
x, ● LOCKHEED TEST DATA
CURVE COMPUTED FROM FIG. 18
EFFECTIVE WIDTH OF SKIN - INS.
COMPRESSIVE STRESS - 1000 P.S.I.

FIG. 21.

TABLE 4

Tests on Simply Supported Curved Sheet

Test No.	t (in.)	b (in.)	Area (sq. in.)	R (in.)	$\frac{R}{t}$	Load (lbs.)	f_{cr} (psi.)	$5E\left(\frac{t}{b}\right)^2$ (psi.)	$*(f_{cr})_{curved}$ (psi.)
1	0.032	24.0	0.768	9.05	283	8,000	10,400	90	10,310
2	0.064	24.0	1.536	9.93	155	34,800	22,650	360	22,290
3	0.032	24.0	0.768	11.7	366	4,800	6,250	90	6,160
4	0.063	24.0	1.512	15.6	244	21,150	13,960	340	13,620
5	0.032	24.0	0.768	14.6	456	5,300	6,900	90	6,810
6	0.063	24.0	1.512	21.8	346	13,900	9,180	340	8,840
7	0.032	24.0	0.768	16.6	519	4,600	6,000	90	5,910
8	0.063	24.0	1.512	23.7	376	13,050	8,620	340	8,280
9	0.032	24.0	0.768	28.7	896	1,920	2,500	90	2,410
10	0.063	24.0	1.512	33.6	533	10,100	6,680	340	6,340
11	0.0415	10.0	0.415	2.05	49	18,675	45,000	860	44,140
12	0.032	24.0	0.768	6.17	193	11,875	15,450	90	15,360
13	0.0398	10.0	0.398	3.95	99	12,900	32,400	790	31,610
14	0.0411	10.0	0.411	3.00	73	12,910	31,400	840	30,560
15	0.064	24.0	1.536	6.17	96	45,500	29,600	360	29,240
16	0.0641	24.0	1.538	8.3	130	38,500	25,000	360	24,640
17	0.064	24.0	1.536	4.5	70	54,500	35,500	360	35,140

* From Eq. (14).

Fig. 17. However, the difference is small and the deviation will be neglected in this discussion.

From Eq. (13)

$$(f_{cr})_{curved} = (f_{cr}) - (f_{cr})_{flat} = f_{cr} - 5E(t/b)^2 = 0.3Et/R$$

Substituting the reduced modulus E_r for the constant modulus E, the equation reads

$$f_{cr} - 5E_r(t/b)^2 = 0.3E_rt/R \qquad (14)$$

To check the validity of this equation in both the elastic and plastic regions, a number of compression tests were conducted on simply supported, curved 24ST alclad sheet. The range of R/t values of the test panels was such that some failures would occur in the elastic region and some in the plastic region. These panels were made of thin sheet 18 inches long with the grain in the direction of this dimension and with a cross-section as shown in Fig. 22. The results of the tests are tabulated in Table 4. Since the values of the stress for the equivalent flat sheet $(f_{cr})_{flat}$ were all of small magnitude due to the high b/t values of the test panels, the constant modulus was used to compute the stress. In all cases this stress was small compared to the total and so any error involved in its compution would be negligible. It should be noted that the values of L/R for all of the panels was greater than 0.5 and consequently the length effect on the test results was eliminated.

In Fig. 23 the test data from Table 4 are compared with the curve for curved sheet determined from the equation

$$(f_{cr})_{curved} = 0.3E_rt/R.$$

The values for f_{cr}/E_r were taken from Fig. 12. The agreement between this curve and the test data is good in both the elastic and plastic regions thus justifying the use of the reduced modulus in the plastic region for curved sheet in compression.

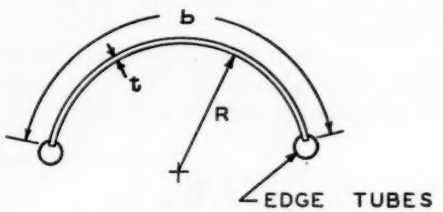

FIG. 22. Cross-section of unstiffened curved panel compression test specimen.

The subject of curved sheet in compression is of such a broad nature that a more complete discussion of this problem is beyond the scope of this paper. Consequently other aspects such as the effect of stiffeners, wave form, and the strength of the sheet after buckling

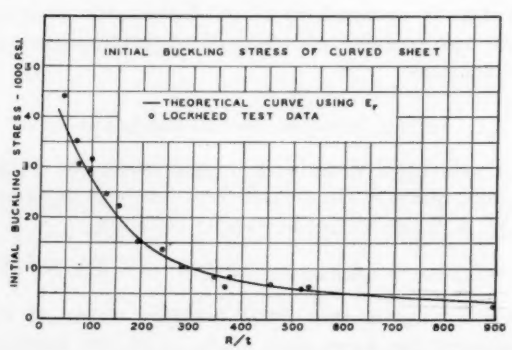

FIG. 23. Comparison of test data with theoretical curve for curved 24ST alclad sheet in compression (with-grain).

will not be discussed here. Likewise no discussion or comparison of other expressions for the initial buckling stress of the curved sheet will be given as this would require too much space.

Conclusions

In conclusion it may be stated that the strength of thin aluminum alloy sheet in the plastic region may be accurately computed by the elastic stability equations if the reduced modulus is substituted for the constant modulus and proper corrections are made for the variations in material stress-strain relationships. Further application of this reduced modulus to other types of structure is not, as yet, warranted pending further studies of the complicated mechanism of failure that occurs in sections of non-rectangular cross-section. However, the work presented in this paper does indicate that, when more intensive studies are made of the various types of failure, the elastic stability equations may be corrected or expanded to account, at least in part, for the behavior of all types of structure in the plastic region.

It might be well to point out here that in the foregoing analyses no account was taken of the local buckling or bowing of stiffeners and its effect on the strength of the supported sheet. It was constantly assumed that the stiffener stress-strain relationships were identical with those of the stiffener materials themselves. Buckling or bowing of the stiffeners further complicates the problem and this fact should always be kept in mind when using the curves or equations presented here for stress analyses. A consideration of strain rather than stress is of utmost importance in the plastic region and it is the author's opinion that greater emphasis should be placed on this in future work. For stress analysis purposes, load *vs.* deflection (if judiciously applied), would be a better criterion against which to measure the strength of a structure rather than stress *vs.* strain. This would especially hold true in the case of a composite structure.

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Comparative Performance of Alcohol-Gasoline Blends in a Gasoline Engine*

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INTRODUCTION

THE OBJECT of this research was to compare the performance of ethyl-alcohol–gasoline blends with the performance of gasoline alone, as fuel for internal-combustion engines. The work was confined to steady-running conditions with engines fully warmed up. It is hoped to cover transient operation, such as starting, warming up and acceleration in a later series of tests.

All tests were made in the Sloan Laboratories for Aircraft and Automotive Engines at the Massachusetts Institute of Technology.

The fuels used consisted of a "regular" grade automobile gasoline of 70 octane number purchased in the open market, and blends of the same gasoline with 10 per cent, 25 per cent and 50 per cent anhydrous ethyl alcohol (by volume before mixing), and 95 per cent ethyl alcohol unblended. The gasoline with 3 cc. of tetra-ethyl lead per gallon was also used in some of the tests. The distillation curves for these fuels are shown in Fig. 1.

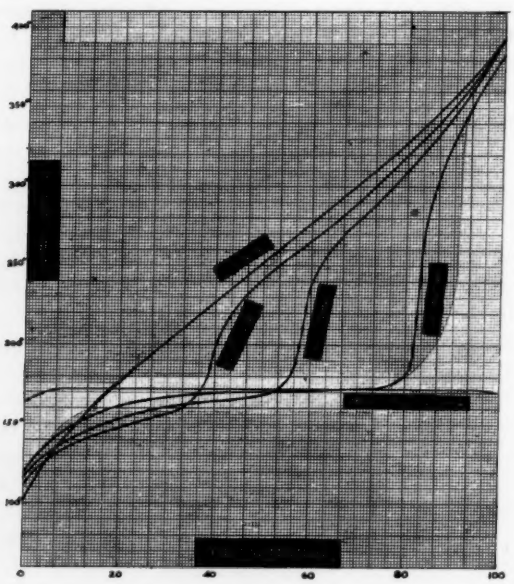

FIG. 1. A.S.T.M. distillation curves of fuels used.

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* This is a condensation of a more detailed report under the same subject, which is on file in the library of the Massachusetts Institute of Technology.

The comparative performance of different fuels in a given engine may be affected by any or all of the following variables:

Speed
Load
Fuel-air ratio
Spark advance
Inlet temperature
Compression ratio

Obviously, to cover all possible combinations of these is impracticable. It is therefore necessary to select a limited number of these combinations for test purposes. The value of the tests will depend to a large extent on how well these combinations are selected; *i.e.*, on whether the selections can be considered as giving a result typical of what may be expected under service conditions.

The selection of speeds and loads to be used in such tests depends on the particular variable being investigated. In determining the required inlet temperature, one is particularly concerned with the problem of distribution and it appears logical to choose speed and load combinations which will result in both as good and as bad distribution as would be likely to occur in practice. In determining compression ratio, detonation is in question, and here combinations of speed and load giving both severe detonation conditions and no detonation should be selected. By such considerations, the speed-load combinations used in this research were reduced to a practicable number.

Fuel-air ratio was considered of such importance as to call for variation over the useful range in nearly every test.

Spark advance was varied sufficiently to establish the optimum for each set of conditions.

Optimum inlet temperatures for each blend were the subject of an extensive investigation (Part 1 of this report), and the inlet temperatures so determined were used in later tests.

A special series of tests (Part 2) was also made in order to determine the best compression ratios to use with each blend.

Finally, when the proper combination of the variables above listed had been determined, performance tests in a typical motor-car engine (Buick 8, 1938 model) were made to compare the performance of the blends under conditions simulating those encountered

in road-vehicle service. The work was divided into several parts, as follows:

Part 1—Determination of optimum inlet temperatures for each blend* in a motor-car engine running at two different speed-load combinations and with various fuel-air ratios and spark timings.

Part 2—Determination of the performance of each blend at various fuel-air ratios, compression ratios and spark timings in a C.F.R. single-cylinder engine, using in each case the optimum inlet temperature for the blend, as determined in Part 1.

Part 3—Determination of the performance of the blends in a 1938 motor-car engine at various fuel-air ratios, using the optimum inlet temperature for each blend as determined in Part 1, and a suitable range of compression ratios as determined in Part 2.

Part 4—Computations of the comparative size, weight, power output and fuel consumption of engines using the various blends under the following assumptions:

(a) No change in engine size, compression ratio or carburetor adjustment.

(b) Same as (a), but with optimum carburetor adjustment for each blend.

(c) No change in engine size, but with optimum compression ratio and carburetor adjustment for each blend.

(d) Relative size of engines for equal power, assuming optimum compression ratio and carburetor adjustment for each blend.

Spark adjustment was assumed to be optimum in each of the above cases.

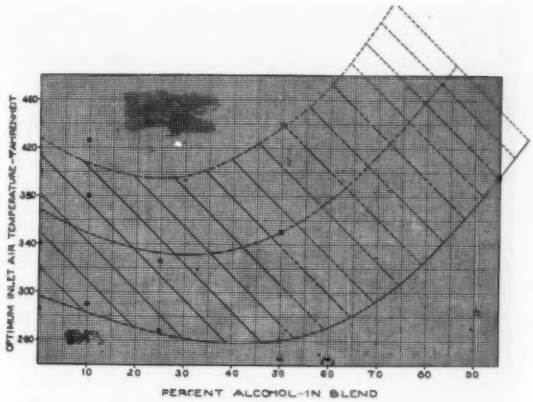

FIG. 2. Variation of optimum inlet air temperature with per cent alcohol in blend. "M" run; 900 r.p.m., full throttle; 11° spark advance; short manifold.

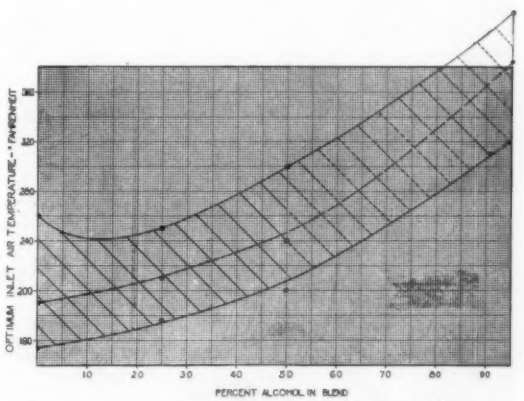

FIG. 3. Variation of optimum inlet air temperature with per cent alcohol in blend. "Z" run; 2000 r.p.m., part throttle; short manifold.

PART 1—TESTS TO DETERMINE OPTIMUM INLET TEMPERATURES

A six-cylinder 1934 Chevrolet engine connected to an electric cradle dynamometer was used for these tests. The manifold heater was removed and was replaced by an external intake-air heater. Two speed-load combinations were used, namely:

(1) 900 r.p.m. at full throttle, which represents "getaway" in slow traffic (or severe hill climbing) at about 18 m.p.h., high gear;

(2) 2000 r.p.m. at part throttle, representing level-road running at about 40 m.p.h. The throttle opening consisted of a fixed orifice which limited the power to about 20 b.hp.

Condition (1) was chosen as being one in which the distribution would be particularly poor, whereas the distribution in condition (2) might be expected to be good.

At each speed-load combination, runs were made at several inlet-air temperatures with each fuel. Two widely different inlet manifolds were used, as well as a fixed spark timing and a variable spark timing (best power with gasoline, without detonation, at each temperature). For each run the fuel-air ratio was varied over the entire useful range. Optimum inlet-air temperature was taken as that giving minimum specific fuel consumption at best-power fuel-air ratio.

Figs. 2 and 3 summarize the results of the tests made in Part 1 with the standard inlet manifold and fixed spark timing. The middle curve in each case represents the optimum inlet temperature, while the shaded area shows the allowable departure from this optimum for an increase of one per cent in specific fuel consumption.

Other combinations of inlet manifold and spark timing tended to raise or lower the temperature scale, but did not change the relative temperatures as between blends.

It was concluded from these tests that, provided the inlet temperature was at least 100°F. for gasoline, the

* In speaking of "blends" in this report, the unblended gasoline and unblended alcohol are included.

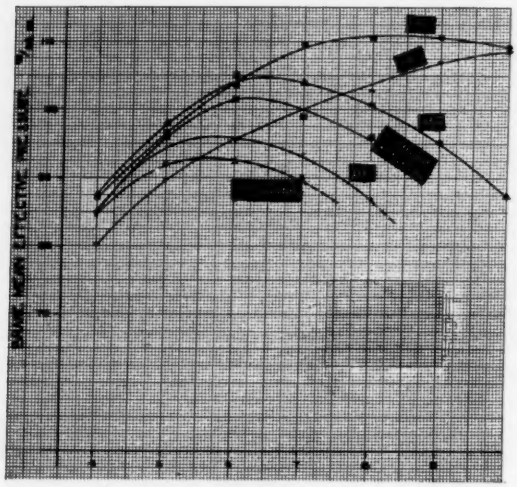

FIG. 4. Effect of compression ratio on the maximum b.m.e.p. obtainable with various blends. 900 r.p.m.; optimum inlet air temperatures; optimum spark advance.

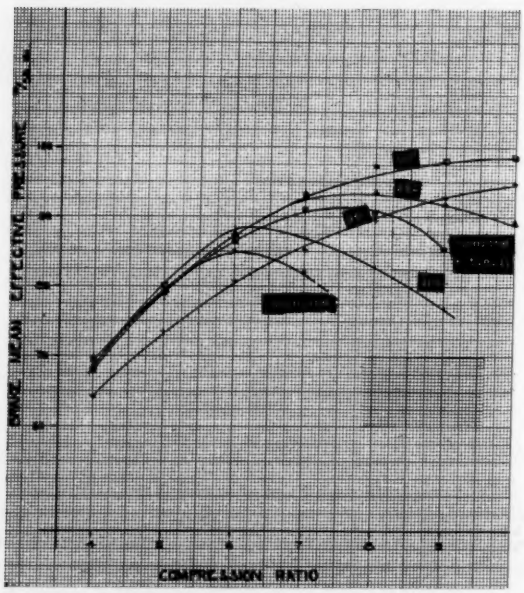

FIG. 5. Effect of compression ratio on the maximum b.m.e.p. obtainable with various blends. 1400 r.p.m.; optimum inlet air temperatures; optimum spark advance.

temperature "schedule" for the other blends should be as follows:

For the 10 per cent alcohol blend, same as for gasoline
For the 25 per cent alcohol blend, same as for gasoline
For the 50 per cent alcohol blend, 30°F. higher than gasoline
For the 95 per cent alcohol, 140°F. higher than gasoline

It was further demonstrated by these tests that while the optimum temperatures varied with running conditions (such as speed, load, fuel-air ratio, etc.), the *relative* performance of the blends was unaffected if the above temperature schedule was maintained. Such a schedule was therefore adopted for subsequent work on this problem.

PART 2—DETERMINATION OF OPTIMUM COMPRESSION RATIO

A C.F.R. engine with variable compression ratio was used for these tests. Power was measured by an electric cradle dynamometer and all usual observations, including air consumption, were made. Humidity was held constant at 160 grains of water per pound of air. The inlet-air temperature schedule was 140°F. for gasoline, 10 per cent and 25 per cent blends; 170°F. for the 50 per cent blend and 280°F. for the 95 per cent alcohol. The gasoline with 3 cc. per gallon of tetra-ethyl lead was also used in these tests, at 140°F. inlet temperature.

Since this part of the work was concerned with the detonation limit of the fuels, all tests were made at full throttle. Two speeds were used, namely, 900 and 1400 r.p.m.

Runs were made at a series of different compression ratios, in each case varying the fuel-air ratio over the useful range. At each fuel-air ratio the spark timing was adjusted to maximum power, or to incipient detonation, whichever appeared first.

For each blend at each compression ratio, curves of brake mean effective pressure *versus* brake specific fuel consumption were plotted. From these curves Figs. 4 to 10 were plotted to summarize the results.

Referring to Fig. 10, it is apparent that the optimum compression ratio for each blend varies with test conditions and with the criterion desired, *i.e.*, maximum output or minimum specific fuel consumption.

Blends up to 25 per cent alcohol, when used at optimum compression ratios, will give as much as ten or twelve per cent more full-throttle power than 70 octane gasoline, with no increase in specific fuel consumption. Although blends of 50 per cent alcohol and above will give still more full-throttle power, the specific fuel consumption increases to such an extent that they appear to have little practical interest. It was therefore concluded that blends up to and including the 25 per cent blend should be investigated in Part 3.

The gasoline with 3 cc. of tetra-ethyl lead per gallon gave full-throttle power slightly inferior to the 25 per cent blend. Full-throttle minimum specific fuel consumption with the leaded gasoline was lower than that with any of the other fuels at compression ratios of 5 and over, being about eight per cent less than the minimum for the 25 per cent blend.

PART 3

Part 3 was essentially a verification of Part 2 on a

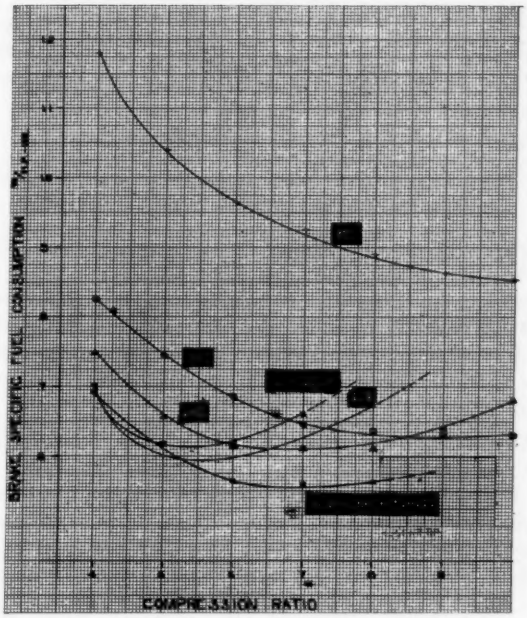

FIG. 6. Effect of compression ratio on the minimum b.s.f.c. obtainable with various blends. 900 r.p.m.; optimum inlet air temperatures; optimum spark advance.

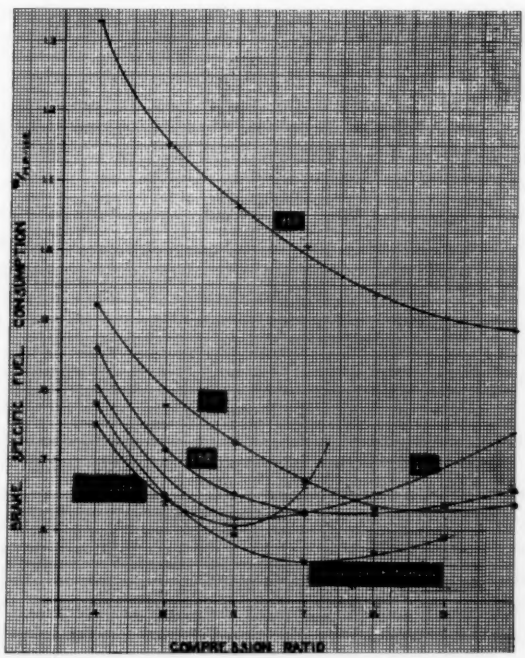

FIG. 7. Effect of compression ratio on the minimum b.s.f.c. obtainable with various blends. 1400 r.p.m.; optimum inlet air temperatures; optimum spark advance.

multi-cylinder engine. The engine used was a 1938 Buick "Series 40" eight-cylinder engine. Only the gasoline, 10 per cent and 25 per cent blends were used.

The following items were held constant for all tests:

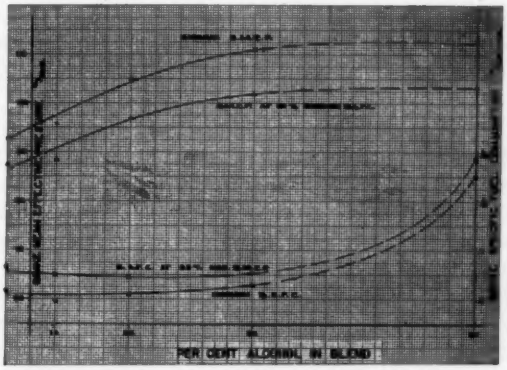

FIG. 8. Effect of per cent alcohol in blend on performance at optimum compression ratio. 900 r.p.m., full throttle.

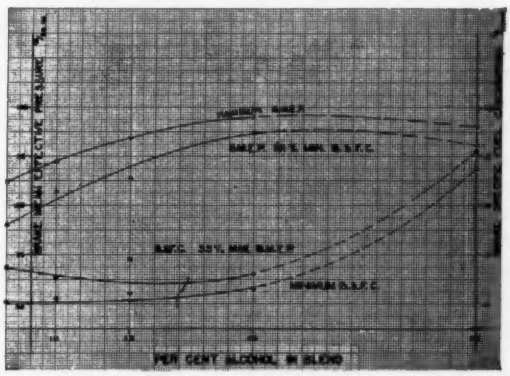

FIG. 9. Effect of per cent alcohol in blend on performance at optimum compression ratio. 1400 r.p.m., full throttle.

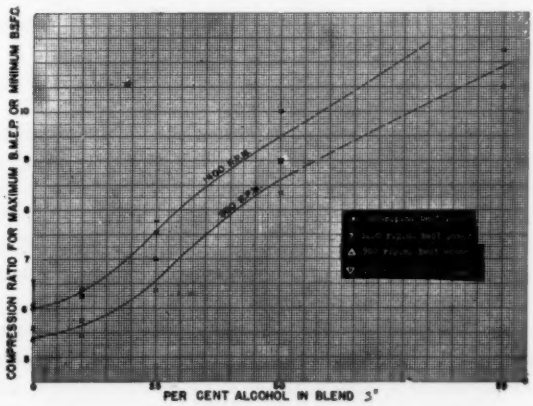

FIG. 10. Variation of optimum compression ratio with per cent alcohol in blend.

Absolute pressure at carburetor inlet, 29.2″ Hg
Absolute humidity at carburetor inlet, 160 grains/pound
Temperature of air at carburetor inlet, 100°F.

The inlet-air temperature was held at 100°F. for all the tests of Part 3 because this was considered to ap-

proach average driving conditions more nearly than the 140°F. inlet temperature used in Part 2. An analysis of the relative performance of the three blends at 100°F. was made from the data obtained in Part 1, and this showed that the *relative* performance would not be appreciably affected by running at 100°F. instead of 140°F. By fixing the temperature, pressure and humidity of the inlet air, the engine became independent of variations in laboratory atmospheric conditions.

For each run of Part 3 the fuel-air ratio was varied over the useful range in about ten steps; and the spark was adjusted for best power or to incipient detonation, at each fuel-air ratio. Each blend was tested in this manner at 900 r.p.m., 1400 r.p.m. and 2000 r.p.m. At each speed, one run was made at full throttle and another run was made with an orifice plate inserted after the carburetor. The sizes of the orifice plates were selected to give an engine output approximately equal to the level-road power required by the motorcar for which the engine was designed.

This entire procedure was repeated at compression ratios of 5.5, 6.5, 7.5 and 8.5, covering the practical range of compression ratios as determined from the tests of Part 2.

As an additional precaution, to reduce the effect of unavoidable variations on the relative performance, all three blends were run at a given combination of engine r.p.m., compression ratio and throttle setting, on the same day.

Results are summarized in Figs. 11, 12, 13 and 14. The general trends and the relative performance at full throttle are similar to those of Part 2. At part throttle as would be expected, performance on all fuels improves with increasing compression ratio, since detonation did not occur within the range of the tests.

Part 4

This is essentially an interpretation of the results of the preceding parts of the investigation. Computations were made for four assumed "conditions" or methods of use of four of the fuels, namely: the 70 octane gasoline; and the same gasoline with 3 cc. of tetra-ethyl lead per gallon, 10 per cent alcohol and 25 per cent alcohol.

For all of this work, "optimum spark timing" was taken as that for best power at a given set of running conditions, or if this resulted in detonation, the spark was considered to be retarded to the point where detonation just disappeared.

Optimum carburetor setting in each case was taken as that which gave the "best-power" fuel-air ratio under the conditions in question. There might be some argument for using mixtures leaner than this at reduced load, but since this would not greatly affect the *relative* performance of the blends, the "best-power" fuel-air ratio was chosen as the basis for all comparisons, both at full throttle and at reduced load.

For each of these conditions, assuming that reliability and durability are not affected by the type of fuel used, the most important criteria for conditions of steady operation are:

(a) Full-throttle engine output at various speeds.
(b) Fuel consumption over the range of speeds and loads most frequently used.

At full throttle, fuel consumption is of little importance. Conversely, the power under throttled conditions can be varied at will, so that the criterion of throttled performance is the specific fuel consumption.

In the following computations, for each of the above-mentioned operating conditions both full-throttle power, and specific fuel consumption when throttled to level-road power, are determined for the four blends at various compression ratios and speeds.

The data for all computations were taken from the primary curves of Part 3 for the gasoline and gasoline-alcohol blends, and from Part 2 for the tetra-ethyl lead blend, since no runs with this fuel were made in Part 3. In this case, correction is made for the difference in friction mean effective pressure of the C.F.R. and Buick engines. While results obtained in this way are not as accurate as if they had been obtained from tests on the Buick engine, it is believed that they show the relative position of the leaded fuel with sufficient precision for the purposes of this report.

Condition 1

This refers to the use of the blends in engines of the same size, with no change in compression ratio or carburetor adjustment, but with spark timing adjusted for each blend. In regions where alcohol blends are only occasionally used, it would be quite feasible to use blends up to 25 per cent without any engine adjustment. However, performance with the alcohol blends can be improved by readjusting the spark timing. Condition 1 assumes that the spark timing control is adjusted to "best power," or to the point of incipient detonation, for each blend, and that the carburetor remains set for "best power"* with gasoline.

The results of the computations for this condition are shown in Table 1 for three compression ratios. It is apparent that under these conditions the alcohol blends in general give slightly greater maximum power with slightly higher throttled specific fuel consumption. The tetra-ethyl lead blend shows better power and the same specific fuel consumption, compared to gasoline.

Condition 2

This refers to the use of the blends in engines of the same size, with no change in compression ratio, but

* "Best-power" fuel-air ratio is defined as the lean fuel-air ratio which gives 99 per cent of maximum power. This is a very definite quantity, while the exact fuel-air ratio for maximum power cannot be determined closely. It is also a good carburetor adjustment, giving improved economy with little loss in power.

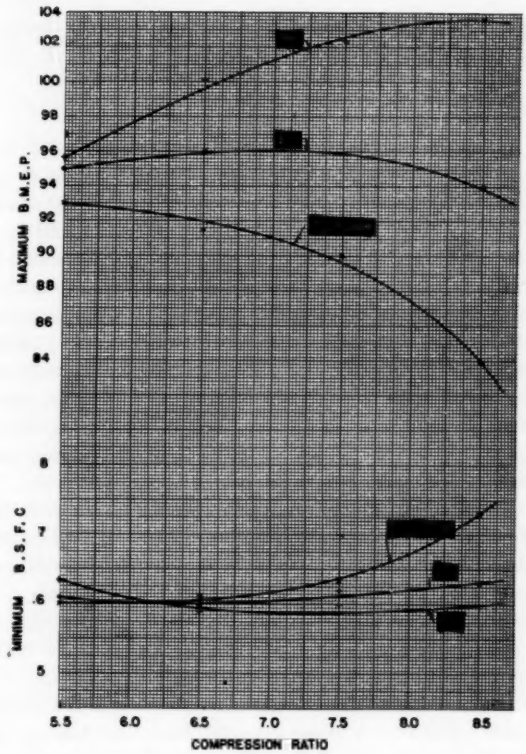

FIG. 11. Effect of compression ratio on maximum b.m.e.p. and minimum b.s.f.c. obtainable; 900 r.p.m., full throttle; optimum spark advance.

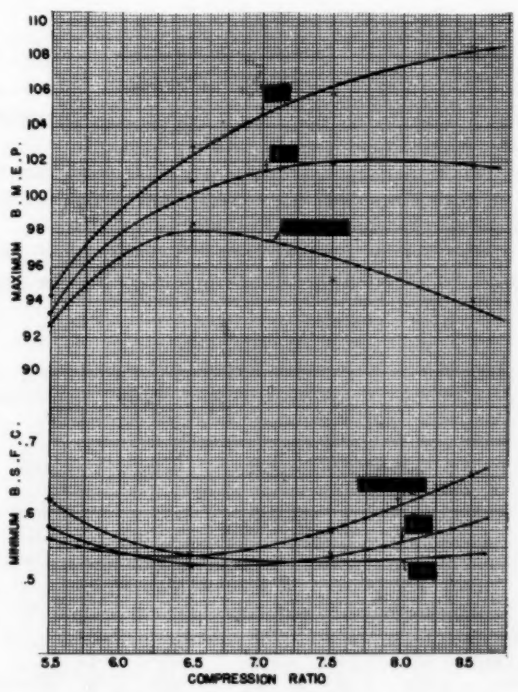

FIG. 12. Effect of compression ratio on maximum b.m.e.p. and minimum b.s.f.c. obtainable; 2000 r.p.m., full throttle; optimum spark advance.

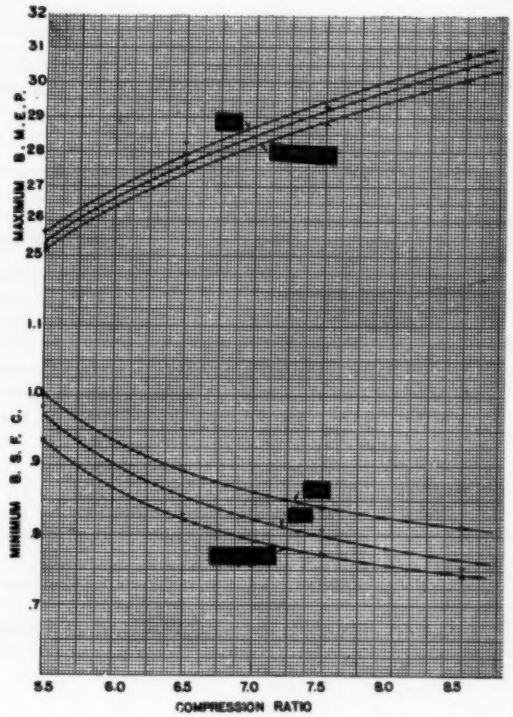

FIG. 13. Effect of compression ratio on maximum b.m.e.p. and minimum b.s.f.c. obtainable; 900 r.p.m., part throttle; optimum spark advance.

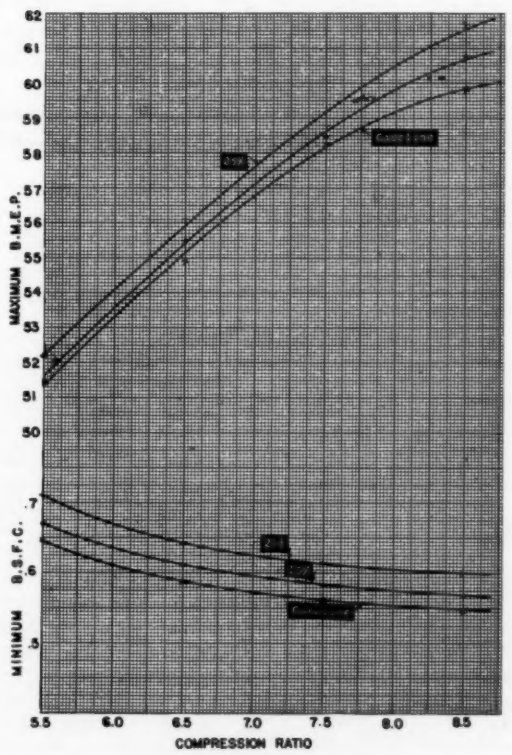

FIG. 14. Effect of compression ratio on maximum b.m.e.p. and minimum b.s.f.c. obtainable; 2000 r.p.m., part throttle; optimum spark advance.

TABLE 1

Relative Full-Throttle B.M.E.P. and Throttled B.S.F.C. at Fuel-Air Ratios Giving 99 Per Cent Best Power with Gasoline (Condition 1)

Compression Ratio	R.P.M.	Relative Full-Throttle B.M.E.P. 10% Blend	25% Blend	Gasoline + 3 Cc. Lead	Relative Throttled B.S.F.C.* 10% Blend	25% Blend
5.5	900	1.011	1.007	1.071	1.020	1.043
	1400	0.995	0.970	1.014	1.019	1.060
	2000	0.994	0.980	...	1.010	1.044
Av.		1.000	0.986	1.043	1.016	1.049
6.5	900	1.052	1.064	1.106	1.004	1.060
	1400	1.033	1.027	1.059	1.033	1.057
	2000	1.026	1.023	...	1.011	1.040
Av.		1.037	1.035	1.083	1.016	1.052
7.5	900	1.050	1.108	1.082	1.010	1.031
	1400	1,071	1.112	1.156	1.002	1.050
	2000	1.073	1.110	...	1.010	1.038
Av.		1.065	1.110	1.119	1.007	1.040

* Corrected to constant power (see Appendix I of full report for method).

NOTE: As no detonation was encountered in the throttled runs, the relative b.s.f.c. with the leaded gasoline will be 1.00.

with both the carburetor and spark timing adjusted for each blend. When alcohol blends are available for use over extended periods of time it would be practical to adjust the carburetor to give "best-power" fuel-air ratio for whatever blend is being used. The spark would also be set to take advantage of the superior anti-knock properties of the alcohol blends.

Table 2 shows the results of these computations. The relative power of the alcohol blends is improved by using the "best-power" fuel-air ratio, but at the expense of throttled specific fuel consumption.

TABLE 2

Relative Full-Throttle B.M.E.P. and Throttled B.S.F.C. at Fuel-Air Ratios Giving Maximum Power with Each Blend (Condition 2)

Compression Ratio	R.P.M.	Relative Full-Throttle B.M.E.P. 10% Blend	25% Blend	Gasoline + 3 Cc. Lead	Relative Throttled B.S.F.C.* 10% Blend	25% Blend
5.5	900	1.020	1.028	1.055	1.052	1.091
	1400	1.000	1.010	1.005	1.063	1.112
	2000	1.006	1.016	...	1.030	1.120
Av.		1.009	1.015	1.03	1.048	1.108
6.5	900	1.050	1.094	1.094	1.074	1.129
	1400	1.046	1.056	1.050	1.086	1.139
	2000	1.025	1.043	...	1.040	1.108
Av.		1.040	1.064	1.072	1.067	1.125
7.5	900	1.064	1.138	1.102	1.052	1.077
	1400	1.066	1.120	1.147	1.090	1.129
	2000	1.069	1.110	...	1.032	1.090
Av.		1.066	1.123	1.125	1.041	1.069

* Corrected to constant power (see full report for method). As no detonation was encountered in the throttled runs, the relative b.s.f.c. with leaded gasoline will be 1.00.

Condition 3

This refers to the use of the blends in engines of the same size, but modified so that each blend is used in an engine having optimum compression ratio and best spark and carburetor settings for that blend. If conditions were such that the alcohol content of blends were standardized at certain unvarying percentages which were always available to the public, it would be advantageous for the engine manufacturer to supply a line of cylinder heads so that engines operating continuously on gasoline could have the standard compression ratio, optimum for gasoline, while engines operating continuously on leaded fuel or on alcohol blends would use higher-compression cylinder heads.

Reference to the results of Parts 2 and 3 shows that the optimum compression ratio for each blend is difficult to select, since it varies with operating conditions and with the criterion used in selection. From the point of view of fuel economy, which is of real importance only at part load, the highest possible ratio should be used with any of the fuels (Figs. 13 and 14). On the other hand, too much sacrifice of full-throttle mean effective pressure, particularly at the lower speeds, is not desirable. This limitation is shown best by Fig. 11, on the basis of which the following optimum ratios were chosen:

Gasoline (70 octane).....6
Gasoline plus 10 per cent of alcohol.....7
Gasoline plus 25 per cent of alcohol.....8

For the tetra-ethyl lead blend, reference was made to Figs. 4 and 5, on the basis of which a compression ratio of 7 was chosen for this fuel.

While these may not be the best possible choice, it is believed that they are at least very close to the proper *relative* values.

Table 3 shows the results of these computations. By choosing the optimum compression ratio for each

TABLE 3

Relative Full-Throttle B.M.E.P. and Throttled B.S.F.C. at Optimum Compression Ratio and "Best-Power" Fuel-Air Ratio for Each Blend

R.P.M.	Relative Full-Throttle B.M.E.P. 10% Blend C. R. = 7	25% Blend C. R. = 8	Gasoline + 3 Cc. Lead. C. R. = 7	Relative Throttled B.S.F.C.* 10% Blend C. R. = 7	25% Blend C. R. = 8	Gasoline + 3 Cc. Lead C. R. = 7
900	1.037	1.118	1.070	1.012	1.027	0.960
1400	1.058	1.127	1.061	1.014	1.046	0.955
2000	1.050	1.113	...	0.996	1.027	0.963
Av.	1.048	1.119	1.066	1.007	1.033	0.959

* Corrected to constant power.

blend, the performance relative to the 70 octane gasoline is considerably improved.

Condition 4

This refers to the use of the blends in engines of such size that the full-throttle output will be the same for each blend. Compression ratio and all other adjustments are optimum for each blend. Since maximum power output determines the size of an engine, a comparison of engines specifically designed for each fuel so that their maximum power would be the same would seem to afford the fairest basis of comparison, for use under conditions where each engine will be used with one fuel only.

If, in a series of geometrically similar engines of different sizes, the product of a characteristic length, l, and the r.p.m., n, is kept constant, it will be found that all of these engines will be operating at the same rubbing velocities, engine stresses and volumetric efficiencies. If the viscosity, μ, of the lubricating oil is also reduced in proportion to the engine size (a reasonable assumption, as it is desired to operate at the same value of μn for reasons of lubrication), it will be found that friction mean effective pressure and brake mean effective pressure will be the same for all the engines at constant values of ln.* Also, the peak of the brake horse power curve occurs at the same value of ln for all engines of such a series. To operate a small engine in a motor-car at the same value of ln as a larger engine, it is necessary to increase the rear-axle ratio in inverse proportion to the dimension, l. As noted in the literature, the power of similar engines running at the same value of ln and the same brake mean effective pressure is proportional to l^2. Therefore, engines of the same power, but having different brake mean effective pressures, will vary in size in such a way that the linear dimensions of each engine are inversely proportional to the square root of the brake mean effective pressure. In other words, the engine dimension needed for constant brake horse power will be proportional to $(1/\text{b.m.e.p.})^{1/2}$ and n, the r.p.m., will be proportional to $(\text{b.m.e.p.})^{1/2}$. This analysis neglects the fact that there would be less tendency toward detonation in a small engine running at higher rotative speed. This effect would operate to the advantage of the small engine, permitting a more advanced spark and therefore more power than calculated. It is not possible to calculate the magnitude of this effect exactly, but it is believed to be too small to do more than accentuate the effect of engine size on the relative fuel consumption by one or two per cent.

The compression ratios used with each blend are those chosen as optimum in Condition 3. Spark and carburetor adjustments are assumed to be optimum.

* See *The Internal Combustion Engine*, Taylor and Taylor, International Textbook Company, 1939.

Since the engines of Condition 4 are the same as those of Condition 3 except for size, and the product, ln, is kept constant at the same road speed, the full-throttle brake mean effective pressures will be the same as those listed in Table 3. The relative engine dimension, l, and displacement needed for constant full-throttle brake horse power output are calculated from the above considerations and are tabulated in Table 4. A similar procedure was followed for the tetra-ethyl lead blend, using data from Part 2.

TABLE 4

Relative Size of Similar Engines for the Same Full-Throttle Brake Horse Power with Each Blend

Fuel	Compression Ratio	Relative Full-Throttle B.M.E.P. (from Table 3)	Relative Engine Size, l	Relative Displacement, l^3
Gasoline	6	1.00	1.00	1.00
10% blend	7	1.048	0.978	0.935
25% blend	8	1.119	0.946	0.847
Gasoline + 3 cc. lead	7	1.066	0.970	0.910

Since the engines of Condition 4 operate at the same product of ln as those of Condition 3, their friction mean effective pressures, mechanical efficiencies and brake specific fuel consumptions may be calculated from those of Condition 3.

Table 5 gives the throttled mean effective pressures obtained at constant throttle position in Condition 3 and the corresponding specific fuel consumptions.

TABLE 5

Throttled B.M.E.P.'s and B.S.F.C.'s Observed at Constant Throttle Position for Condition 3

	Throttled B.M.E.P.			Uncorrected Throttled B.S.F.C.		
R.P.M.	Gasoline C. R. = 6	10% Blend C. R. = 7	25% Blend C. R. = 8	Gasoline	10% Blend	25% Blend
900	26.2	28.2	29.8	0.865	0.850	0.847
1400	42.9	46.5	49.0	0.664	0.655	0.665
2000	52.8	56.6	60.0	0.630	0.612	0.623

Table 6 lists the throttled mean effective pressures required with the different size engines of Condition 4 to give constant throttled power equal to that obtained with gasoline.

Table 7 gives the relative throttled specific fuel consumptions of the different size engines, computed from the fuel consumptions of Table 5, corrected for the differences in mean effective pressure.* In this case, the alcohol blends show an improvement in

* For details of this correction, see original report.

TABLE 6

Throttled B.M.E.P.'s Required with Variable Size Engines of Condition 4 to Give Constant Throttled Power Equal to That Obtained with Gasoline

	Required Throttled B.M.E.P.		
R.P.M.	Gasoline (Table 5)	10% Blend (Gasoline × 1.048)*	25% Blend (Gasoline × 1.119)*
900	26.2	27.4	29.3
1400	42.9	44.9	48.0
2000	52.8	55.2	59.0

* See Table 4.

TABLE 7

Throttled B.S.F.C.'s of Table 5 Corrected for Throttling from the B.M.E.P.'s of Table 5 to the Required B.M.E.P.'s of Table 6

	Corrected B.S.F.C.			Relative B.S.F.C.		
R.P.M.	Gasoline (Table 5)	10% Blend	25% Blend	10% Blend	25% Blend	Gasoline + 3 Cc. Lead
900	0.865	0.844	0.840	0.986	0.983	0.932
1400	0.664	0.647	0.661	0.975	0.995	0.933
2000	0.630	0.607	0.620	0.964	0.985	0.942

specific fuel consumption only exceeded by the tetra-ethyl lead blend. The 25 per cent blend requires an engine of only 84.7 per cent of the piston displacement of the engine of equal power using 70 octane gasoline.

GENERAL CONCLUSIONS

1. When used in a typical motor-car engine without any change except spark adjustment, the addition of 10 per cent and 25 per cent of ethyl alcohol to a 70 octane gasoline resulted in

(a) increased full-throttle power with compression ratios over 5.5;
(b) increased part-throttle specific fuel consumption, as compared to the unblended gasoline.

Under these conditions, the addition of 3 cc. of tetra-ethyl lead per gallon to the unblended gasoline resulted in about the same increase in full-throttle power as was realized with the 25 per cent blend. The part-throttle specific fuel consumption was the same as that of the unblended gasoline.

2. When used in a typical motor-car engine with fuel-air ratio and spark adjusted for each blend, the addition of 10 per cent and 25 per cent of ethyl alcohol to a 70 octane gasoline resulted in

(a) increased full-throttle power;
(b) increased part-throttle specific fuel consumption, as compared to the unblended gasoline.

Under these conditions, the addition of 3 cc. of tetra-ethyl lead per gallon to the unblended gasoline resulted in about the same increase in full-throttle power as was realized with the 25 per cent blend. The part-throttle specific fuel consumption was the same as that of the unblended gasoline.

3. When used in a typical motor-car engine with optimum compression ratio, fuel-air ratio and spark timing for each blend, the addition of 10 per cent and 25 per cent of ethyl alcohol to a 70 octane gasoline resulted in

(a) increased full-throttle power;
(b) a slight increase in specific fuel consumption, as compared to the unblended gasoline.

Under these conditions, the addition of 3 cc. of tetra-ethyl lead per gallon to the unblended gasoline resulted in

(a) an increase in full-throttle power intermediate between those realized with the 10 per cent and 25 per cent alcohol blends;
(b) a part-throttle specific fuel consumption lower than with any of the other fuels.

4. When used in a series of similar engines specifically designed and adjusted for each blend, and of such sizes as to develop the same full-throttle brake horse power, the addition of 10 per cent and 25 per cent ethyl alcohol to a 70 octane gasoline resulted in slightly lower part-throttle specific fuel consumption as compared to the unblended gasoline. Under the same conditions, the addition of 3 cc. of tetra-ethyl lead per gallon to the unblended gasoline resulted in a lower part-throttle specific fuel consumption than with any of the other fuels.

5. The optimum compression ratios for the multi-cylinder engine used, based principally on good low-speed power, are approximately six for the gasoline, seven for the 10 per cent blend, eight for the 25 per cent blend and seven for gasoline plus 3 cc. of tetra-ethyl lead per gallon.

6. No operating or starting trouble was encountered in the use of the alcohol blends, but no tests were made with the atmospheric temperature below 60°F. Further tests should be made to determine relative cold-weather starting ability.

7. Alcohol blends would be uneconomical compared to gasoline under any engine operating condition, unless the price of the blends was at least as low as that of gasoline. The specific gravity of the 10 per cent and 25 per cent blends is near enough to that of gasoline (1 to 1½ per cent difference) so that this conclusion is valid on a volume or weight basis.

Light Aircraft Engine Developments

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Introduction

Shortly after the first World War considerable interest arose in experimental and development work on gliders. Glider Meets were organized and as a result of continued research work the movement grew as a sport and the technique of soaring flight was evolved. In 1922 the world was astounded by the news of remarkable extended soaring flights of several hours without power.

The results of these glider trials pointed the way to new possibilities in low-powered sport airplane development and gave rise to the idea of altering the design of this form of craft sufficiently to install a small engine, and we had what was then called a powered-glider. For this work, existing motorcycle engines of from 3 to 12 hp. were used and an era of competitions and active developments resulted.

Fig. 1 shows two of these converted engines which were actively used at that time.

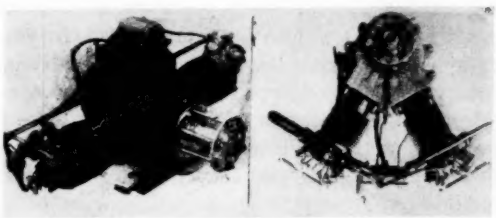


Fig. 1.

The more serious minded aeronautical engineer looked upon all of this work very skeptically, and was certain that nothing worth while would ever come out of it. However, it was the beginning of the so-called *light airplane* of today.

Those engaged in the work saw in it the real possibility of the present-day economical low-priced airplane which would make it possible for anyone to learn to fly and even own an airplane for their private use if they so desired, and it is due to the faith and courage of that pioneering work that we now have the light airplane industry.

Additional work brought out the fact that these small airplanes had to be more ruggedly constructed to withstand everyday use and that motorcycle engines lacked sufficient power and durability to meet the requirements.

Presented at the Power Plants session, Ninth Annual Meeting, I.Ae.S., New York, January 20, 1941.

In considering the requirements of new engines for this use at that time the airplane builders decided that 25 to 30 hp. should be sufficient to enable them to build a good performing airplane.

Numerous new small engines became available on the market in varying types and sizes, but the two-cylinder opposed engine seemed to take hold due to its extreme simplicity of design, relatively smooth operation and low cost, and the fact that it was apparently the most elementary sort of engine "good enough to do the job."

Undoubtedly, the precedent set up at that time is one of the underlying reasons for the almost universal use of the opposed type of engine in the present-day light airplane.

Power requirements, naturally, increased and in due course a large well known manufacturer of automotive engines, visualizing the possible future market for the small commercial airplane, introduced the first four-cylinder 35 to 40 hp. opposed type engine for aircraft use in this country.

Automotive engine features were employed extensively in its design and it was undoubtedly the first light aircraft engine which was adequately tooled for low-cost production.

It was naturally much smoother in operation and gradually replaced the former two-cylinder engine entirely, partly because it could be purchased at a lower price due to better manufacturing facilities.

The light airplane manufacturers definitely established a future for instruction and private flying with airplanes equipped with these engines, and as a result the light airplane industry rapidly became a substantial growing business. Throughout this development period the air-cooled type of engine has predominated, and very little use has been made of liquid-cooled engines.

There has been a diminishing interest in the small radial type engine because it was not as smooth in operation, not as clean an installation aerodynamically, and does not afford as good vision. The possibility of the two-cycle engine with its inherent simplicity has always held a sort of hopeful place during this whole development period, but as yet no one has actually introduced anything which has constituted any serious threat to the continued use of its more favored, four-cycle brother.

In competition, other automotive engine manufacturers entered the light airplane engine field, evidently confident that a considerable volume of these engines would eventually be built and sold annually. The

market has been growing approximately 50 per cent each year over the preceding one and there are now several thousands of these small engines in daily use where they have established a very creditable record for dependability, economy of operation and freedom from attention in service.

Type

The opposed type of engine seems to be much preferred at the present time due to its better inherent balance and smooth running characteristics, slight weight advantage and ease of installation. There is almost universal agreement that it fits into the present trend of airplane design much better, everything considered, than any other type. The opposed type engine is shorter in overall length, which gives considerably better range of vision, and the overall width is quite easily kept within the limitations of the required breadth of the airplane for adequate seating comfort. This is made possible by careful selection of Bore-Stroke sizes and compact engine design within the range of power requirements so far considered. The opposed type engine also lends itself to more economical production than other types having a similar number of cylinders. Proof of this is shown by the fact that all of the competitive engines for this class of service at the present time are of the opposed type.

Present Trends of Design

The basic design arrangements of the competitive engines are strikingly similar at the present time.

Crankcases are split vertically on the engine centerline with through-bolt construction for holding the halves together. This results in a good sound design from the rigidity standpoint and the castings are of a simple nature.

The crank and camshaft bearings are on the crankcase split which makes for ease of assembly.

Crankshafts are of the 3-bearing 4-throw type, machined only on the crankpins and journals as in automotive practice.

The pins and journals are usually lightened by drilling internally and the resulting shafts are carefully balanced statically and dynamically by grinding away material from the crankcheeks. On some models, crankshaft journals are hardened by nitriding, which materially increases bearing life by preventing shaft scratching due to the lack of good oil filtration. Camshafts are kept low in price by machining the bearings and cam faces only.

Due to the fact that this type of engine consists of multiples of 2-cylinder opposed engines, the arrangement naturally works out so that valve timing can be accomplished by the use of only one intake cam and two exhaust cams for each opposing pair of cylinders, which only necessitates 6 cams for a 4-cylinder engine and 9 for a 6-cylinder engine. This helps in the reduction of cost.

Conventional forged steel connecting rods are used, machined only at the crank pin and piston pin bearing bores in accordance with automotive practice.

Precision, interchangeable-type, steel backed, lead-bronze main and crank pin bearings are used and plain thrust bearings seem adequate to meet the requirements so far. Camshafts operate entirely satisfactorily directly within the aluminum crankcase bores, even with splash lubrication.

Permanent-mold aluminum pistons of conventional solid trunk type design are used and automotive grade piston rings have proved adequate in service.

Valve in head cylinder design is now universal, using conventional cast aluminum heads and either cast iron or steel cylinder barrels screwed, shrunk or bolted to the heads. For this work it is necessary to use a considerably larger bore than stroke, which also assists in getting adequate valve sizes in a sort of roof type of combustion chamber having valves straight in the head. This results in a straightforward manufacturing job with simple tooling. Careful design is maintained on ports and cooling fins to insure good output and adequate cooling capacity, and down exhaust is now universally used for this type of engine.

Aluminum bronze intake and Austenitic steel exhaust valve seat inserts are shrunk in place.

Hydraulic tappets are now being used in many engine designs and the entire valve operating mechanism is completely enclosed, and lubricated from the engine pressure lubrication system. Positive lubrication of the valve mechanism has probably contributed more to the trouble-free operation of these engines in service than any other one single improvement when compared with the older engines, and is very cheaply and simply accomplished.

The accessory arrangements are kept to the very minimum to meet the requirements and straight spur gear drive of the simplest possible design is used.

The airplane manufacturer insists upon wet sump type engines for simplicity of installation and considerable work has been required to control the oil temperatures in hot weather by employing internal baffles to insure circulation and by finning the oil sump both internally and externally.

Single gear-type pressure oil pumps are used, usually running at less than engine speed, and full pressure lubrication is supplied to all bearings and hydraulic tappets by a simple drilled passage system within the crankcase castings.

Oil pressure regulating valves are now located at the extreme forward end of this circuit to avoid a dead-end system and prevent the lodgment of dirt.

Most of the earlier engines of this type were cooled by simply exposing the cylinders to the slip stream using simple individual sheet metal air scoops on either side to direct some air around the back cylinder. In keeping with better external appearance nearly all of the installations at present totally enclose the engine

and a closed pressure system is used together with such simple baffling as may be required to effect satisfactory cooling. With present airplane designs, cylinder and oil temperatures can be more readily controlled in the enclosed type cowlings used.

Two methods prevail for mounting the engine in the airplane—the conventional 4-point platform type where the engine sits on two bed rails, and the other by mounting from the rear of the engine to suitable lugs provided on the crankcase halves.

The latter seems to be desired by the majority of airplane manufacturers because of the fact that it is an easier and lighter structure for them to build and apparently works out entirely satisfactorily in service.

The removable type propeller hub is rapidly being replaced by the integral flanged end crankshaft to which the propeller is directly bolted. This construction is lower in cost, lighter in weight and avoids the care required to properly fit hubs on tapers and the attendant difficulties of removing the hub when required.

The carburetor and magneto companies have done a good job in producing small, dependable and low-priced accessories for use on these engines, and commercial automotive spark plugs are apparently meeting the requirements in most engines.

Compression ratios of 6 to $6^1/_2$ to 1 seem to operate satisfactorily on 73 octane gasoline and newer designs are now going as high as 7 to 1 and over, using 80 octane fuel. Surprisingly good specific outputs and relatively low fuel consumptions are obtained as will be observed by the accompanying performance curves which are average for these engines. This economy is due largely to the form of induction system used which seems to give very good distribution and which lends itself to the possibility of investigating intake pipe sizes and taking full advantage of "tuning" the system to get the maximum power.

The Lycoming O-145 and GO-145 series engines include three models of 4-cylinder opposed type light airplane engines, rated from 50 to 75 hp.

Fig. 2 shows the 50- and 65-hp. engines, which are both direct-drive models rated at 2300 and 2550 rpm,

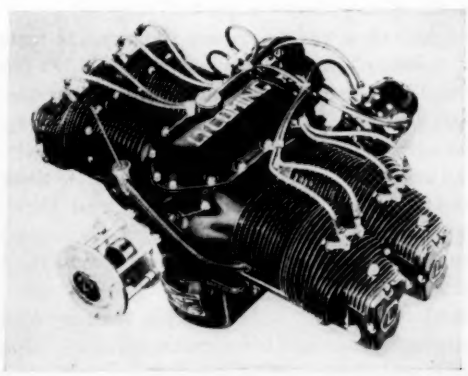


FIG. 2.

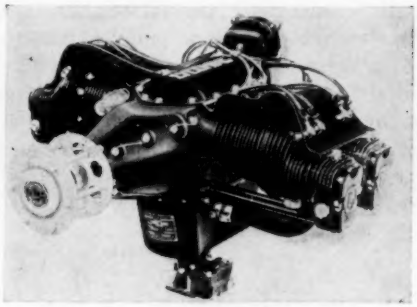


FIG. 3.

respectively, and the 75-hp. engine (Fig. 3) is a geared model of the same design rated at 3200 rpm with a propeller speed of approximately 2000 rpm.

This series of engines comprises essentially one unusual feature of design in that the cylinder barrels and half crankcase are one integral alloy-iron casting, much as in automotive practice. (This is shown in Fig. 4.)

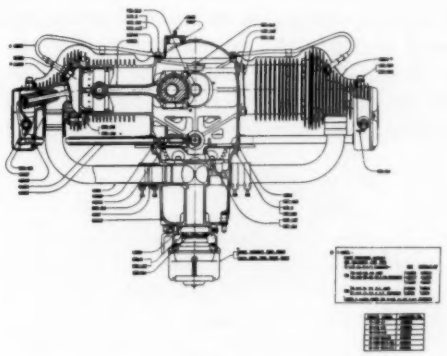


FIG. 4.

The weight was kept down by ingeniously making the crankcase portions of this casting a mere skeleton comprising the main bearing diaphragms and incorporating large openings both top and bottom when assembled together. The top opening being closed by a light cast aluminum cover and a cast aluminum oil sump is attached to the bottom (as shown in Fig. 5).

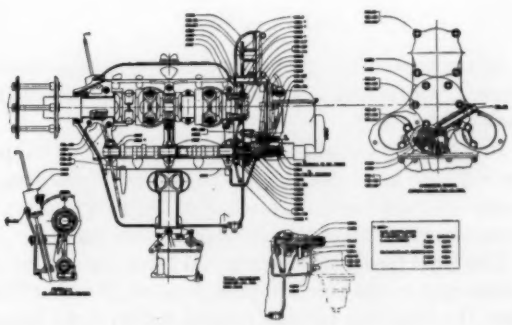


FIG. 5.

This results in an engine of normal weight ratio and has proved in service to be a sound practical construction.

As will be seen in Fig. 6, the gearing for the geared model consists of an internal gear mounted in separate bearings driven by a pinion which is directly attached to the crankshaft. It was found necessary during the

LONGITUDINAL SECTION THRU MODEL GO-145
GEARED ENGINE

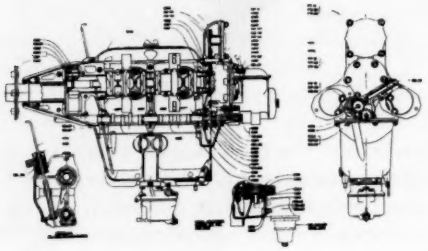


FIG. 6.

development to use pendulum type dampers in order to effect smooth, quiet operation of the system, but in all other respects this engine is identical with the ungeared models and is made largely over the same tools. Some 3500-r.p.m. engines have now been built and are in regular daily service. Figs. 7 and 8 show performance curves and specifications of these engines.

PERFORMANCE CURVES
O-145 & GO-145 ENGINE

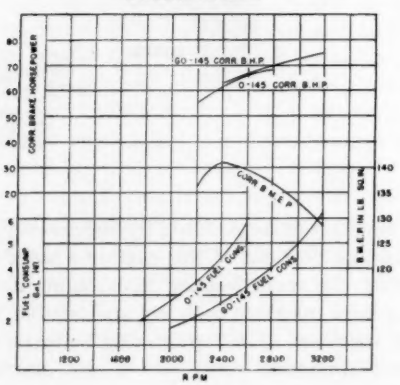


FIG. 7.

ENGINE SIZES

As the popularity and use of the light airplane has increased, it is natural that more and more is expected of their power plants.

In order to meet the requirements for additional performance, more comfort and increased equipment, power outputs have increased from 40 to 80 hp. to include the three-place ships of the last two years.

The need for more power is still going up and no one knows just where this will tend to level off and still say that the resulting engines remain within light aircraft classifications. This makes it very difficult in designing

GENERAL SPECIFICATION OF
LYCOMING O-145 SERIES ENGINES

MODEL	O-145-A1 DIRECT	O-145-B1 DIRECT	O-145-C1 DIRECT	GO-145-C1 GEARED
ENGINE TYPE CERTIFICATE	199	210	210	210
RATED HORSEPOWER	50	65	75	75
RATED R.P.M.	2300	2550	3100	3200 CRANKSHAFT 2048 PROPELLER
COMPRESSION RATIO	5.65:1	6.5:1	6.5:1	6.5:1
MINIMUM OCTANE FUEL	65	73	73	73
CRUISING FUEL CONS. #/HP/HR.	0.55	0.50	0.50	0.50
OIL CONSUMPTION-MAX. #/HP/HR.	0.01	0.01	0.01	0.01
OVERALL WIDTH - INCHES	29.56	29.56	29.56	29.56
LENGTH - PROP. FLANGE TO REAR	23.75	23.75	23.75	28.44
OVERALL HEIGHT - INCHES	20.59	20.59	20.59	20.67
WEIGHT, LB., SINGLE IGNITION MINUS PROPELLER HUB AND CYLINDER AIR BAFFLES	152	155	155	181

SPECIFICATIONS - ALL MODELS

TYPE	AIR COOLED, TWO BANK, HORIZONTALLY OPPOSED
NO. OF CYLINDERS	4
BORE - INCHES	3.625
STROKE - INCHES	3.500
DISPLACEMENT - CU. IN.	144.5

FIG. 8.

new models to know just what size engine to figure on or what the production possibilities for any new model might be.

It is true that in the past, very few engines that were designed for a particular airplane proved large enough for the job after the airplane got into production. This has usually been caused by the fact that better performance and additional equipment are usually desired during the early experimental flight tests, than were originally intended, which necessitates still more power. In an attempt to anticipate the future demand and these ever-changing requirements, about a year ago a more or less broad pattern of engines was designed starting with a 100-hp. 4-cylinder opposed model. By substituting larger size cylinders and pistons this engine was made into a 125-hp. model. This was followed by a 6-cylinder opposed model using the same two sizes of cylinders and pistons and all other allied parts were kept absolutely interchangeable between the two engines by merely making a 6-cylinder crankcase, crankshaft, camshaft and oil sump.

In the design of these new larger engines, every attempt has been made to maintain the same general principles of design, workmanship and low cost per hp. as their smaller predecessors.

This gives two sizes of both 4- and 6-cylinder engines which can be made over essentially the same tool setup, and four engines in increments of 25 hp. with the maximum interchangeability of parts.

This spread of engines allows the airplane manufacturer a wide choice of horsepower to suit his requirements and makes a good setup for low-cost manufacture.

Figs. 9, 10, 11, 12, 13 and 14 show three views each of the new 4- and 6-cylinder engines, respectively. As will be seen from the accompanying figures, these new engines are of orthodox construction and they show quite clearly the manner in which the extensive interchangeability has been accomplished between the 4- and 6-cylinder engines.

Crankshaft journal and crankpin sizes are identical, consequently the main bearings are all alike. Connecting rods and pistons are likewise alike.

The crankcase cross-section and cylinder spacing is

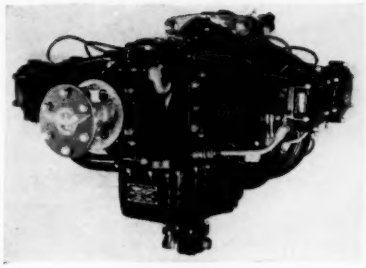


FIG. 9.

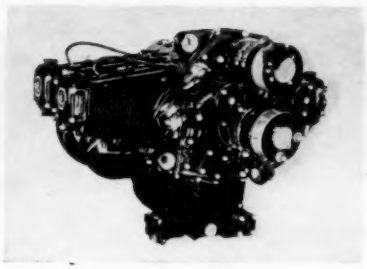


FIG. 10

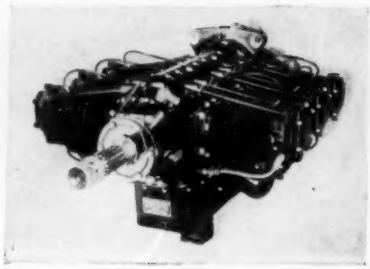


FIG. 11.

the same, as well as the rear accessory housing flange.

All crankcase studs for holding the halves together are alike.

The openings in the crankcases for the cylinders are machined to take the large bore cylinder and a sort of false pilot is provided on the small bore cylinder flange to fit this same opening. The two sizes of cylinders are identical in general design and the valves and all of the associated valve gear parts throughout are alike for both size cylinders.

The intake and exhaust cam profiles are identical, consequently one master cam grinds all cams in either model engine.

All gears are alike between the two engines with the exception of the magneto driving gears which cannot be the same on account of the required difference in speeds for timing between a 4- and a 6-cylinder engine.

The entire oil pump and all its associated lubrication system parts are alike.

The entire rear accessory housing assembly is identical between the two engines with the exception of the magnetos caused by the difference in gear ratios.

It is interesting to note that the total weight of the accessories on the 4-cylinder model is 23 per cent of the engine weight and 16 per cent on the 6-cylinder.

The oil sumps are naturally different castings due to length, but much of the same tooling is used for machining either model part.

Carburetors and magnetos naturally are not alike due to difference in required sizes and types.

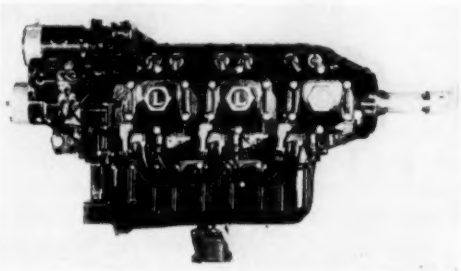


FIG. 14.

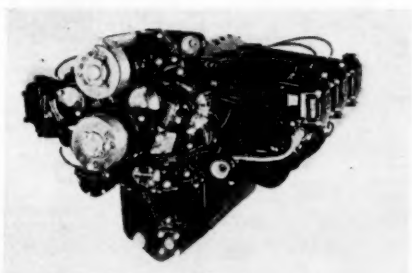


FIG. 12.

FIG. 15.

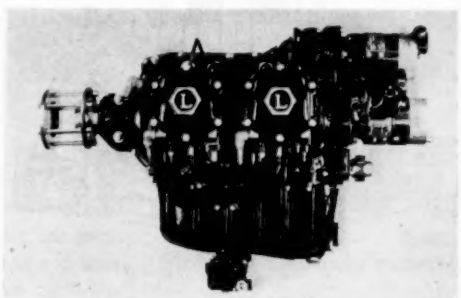


FIG. 13.

FIG. 16.

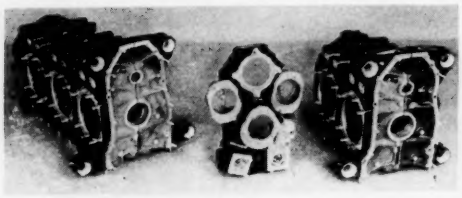


FIG. 17.

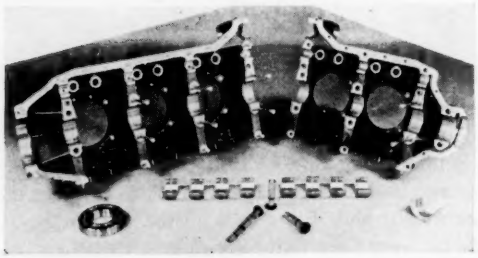


FIG. 18.

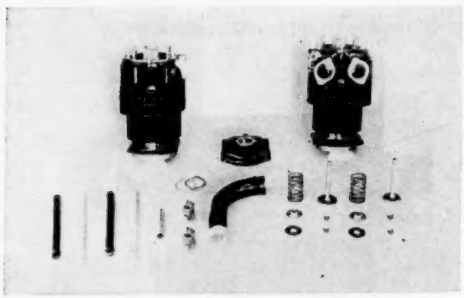


FIG. 19.

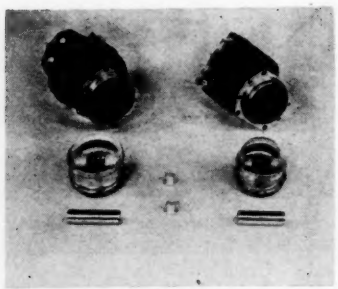


FIG. 20.

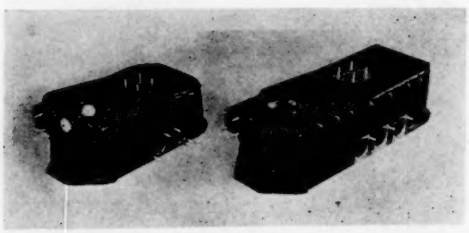


FIG. 21.

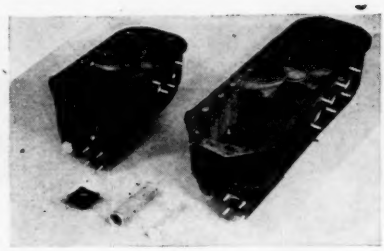


FIG. 22.

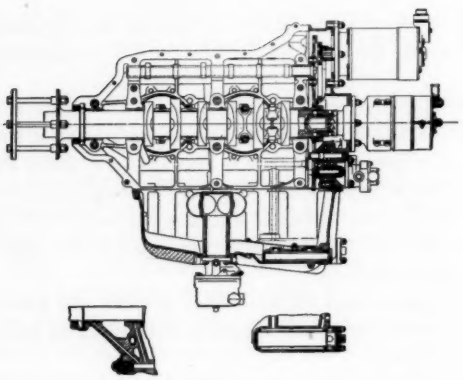


FIG. 23.

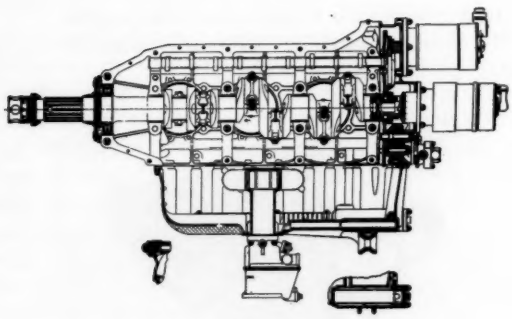


FIG. 24.

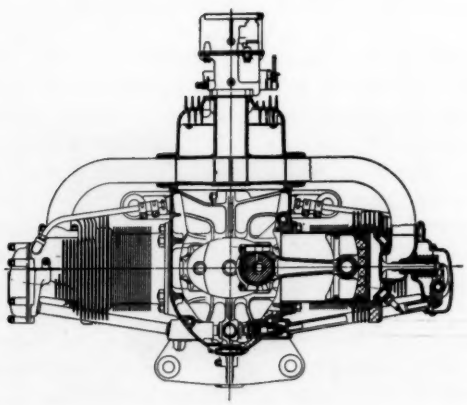


FIG. 25.

Figs. 15, 16, 17, 18, 19, 20, 21, 22, 23, 24 and 25 show these various points of construction.

Fig. 26 shows performance curves of these new engines.

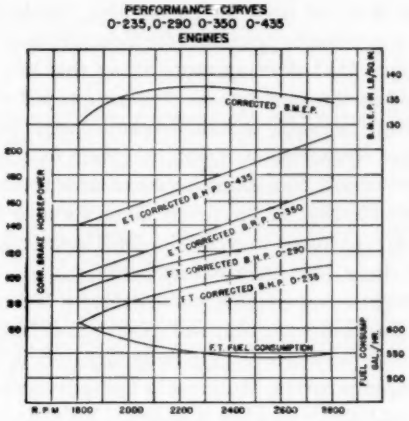


FIG. 26.

Fig. 27 shows the major specifications of these new engines.

This new line of engines rated from 100 hp. to 175 hp. will assist in the further development of faster, larger and more comfortable planes for popular use.

GENERAL SPECIFICATIONS
NEW SERIES LYCOMING ENGINES
AIR COOLED, TWO BANK, HORIZONTALLY OPPOSED

MODEL	DIRECT 0-235	DIRECT 0-290	DIRECT 0-350	DIRECT 0-435
ENGINE TYPE CERTIFICATE				
NO. CYLINDERS	4	4	6	6
BORE INCHES	4.375	4.375	4.375	4.875
STROKE INCHES	3.875	3.875	3.875	3.875
DISPLACEMENT CU. IN	233	289	350	434
RATED H.P.	100	125	150	175
RATED R.P.M.	2500	2500	2500	2500
COMPRESSION RATIO	6.25	6.25	6.25	6.25
MIN. OCTANE FUEL	73	73	73	73
CRUISING FUEL CONS. LB./HP/HR.	.525	.525	.525	.525
OIL CONS. LB./HP./HR.	.01	.01	.01	.01
OVERALL WIDTH INCHES	32.38	32.38	32.38	32.38
LENGTH INCHES	35.19	35.19	40.88	40.88
OVERALL HEIGHT INCHES	25.0	25.0	26.25	27.75
WEIGHT-LB. DUAL IGNITION BUT WITHOUT OTHER ACCESSORIES	223 WITH PROP. HUB	233 WITH PROP. HUB	305 WITHOUT PROP. HUB	332 WITHOUT PROP. HUB

FIG. 27.

REQUIREMENTS

In the design and manufacture of engines for this service, the major factors of importance are:

The lowest possible unit price to the airplane manufacturer.
Smoothness of operation.
Absolute reliability and long service life.
Economy of operation.
Lowest possible weight.
Freedom from attention in service.
Ease and simplicity of maintenance when necessary.
Ease of installation in airplane, simplest possible engine mount, cowling, connections, controls, etc.

Production costs naturally depend on the ingenuity of the basic simplicity of design, ease of assembly, quantity of engines which can be made and the effectiveness of the manufacturing setup to produce it.

Adequate up-to-date equipment and tooling must be available to ensure low piece prices, quick assembly, and parts' interchangeability.

The engine should incorporate the fewest possible number of pieces and each piece must be made from the lowest priced raw materials that will do the job, and also be designed so that it can be produced by as inexpensive equipment as possible.

The design of all castings must be studied carefully for simplicity and cost per piece. Die castings and sheet metal stampings must be used wherever possible.

The raw material cost for one of these engines constitutes approximately one-half of the manufacturing cost, which means that we must be continually trying to reduce this major item by testing to determine what will be good enough for the job.

Proper inspection and control of the major stressed parts of the engine are necessary, and good finish must be maintained on all vital working parts throughout the engine. Hand operations are minimized wherever possible and exterior finish is confined to one coat of paint.

There is a genuine interest in fuel injection on account of smoothness of operation, freedom from icing and acrobatic training requirements.

Dual ignition is rapidly becoming standard equipment, and the absolute necessity for some form of engine starter is at hand, with a distinct leaning toward the adoption of the conventional automotive type electric starter and generator.

Altogether, the task of producing these engines is rapidly becoming one of trying to evolve the simplest and cheapest possible details of every component part, the proof testing and use of the lowest priced materials, careful shopping for the best sources on purchased parts, tooling of each component part for the lowest possible labor costs and the general acceptance of its design features to ensure good demand.

WHAT ABOUT THE FUTURE IN LIGHT AIRPLANE ENGINES?

Much remains to be done in the way of giving the owner more for his investment which will, naturally, come with increased volume and subsequent lower costs of production. The selling price of the airplane will always remain a vital factor on the growth of this business.

There can be no doubt that the light airplane is rapidly becoming the "every man's automobile of the air" and engine requirements will undoubtedly follow along much the same lines.

Muffling of engines is here, the trend for quieter operating engines is under way and smoothness of engine operation is receiving more and more attention.

The day of the open cockpit airplane is past and naturally only limited measures can be taken to dampen and insulate cabin noise and vibration. It is felt that much remains to be done on possible new method of engine mounting to effect greater smoothness of operation and improved passenger comfort which will minimize strain and fatigue on extended flights.

The 6-cylinder engine will undoubtedly find instant favor on account of its smoothness of operation and general running characteristics; however, it is felt that the larger 4-cylinder models will continue to prove useful for some time to come because of their simplicity, shorter length, low cost and slight weight advantage, and that methods of mounting may be evolved that will make them very acceptable in operation.

In view of the present trend toward adoption of electric starters and generators which will necessitate a battery, it is felt that battery ignition will eventually follow.

There is a very definite need for the adoption of some form of oil filter and carburetor air cleaner to meet the varied conditions of service under which these engines operate.

Propeller reduction gearing is already here and several future developments are under way along this line. Supercharging will probably come; however, there are no suitable inexpensive superchargers available at the present time for low horsepower engines.

There is a real need for inexpensive, fundamentally simple, controllable propellers for light airplane use and it is hoped that developments along this line will be available in the near future. Such a propeller will be an absolute necessity as light airplane speeds increase.

Serious consideration must be given to still better vision as airport congestion becomes more involved and it is possible that in the future some form of inverted engine arrangement may be the final answer following further study of installation analysis.

The light commercial airplane industry has cut out a big job for itself—one that will require close cooperation between airplane and engine manufacturers in order to make rapid progress. Interest in flying is increasing by leaps and bounds and the way keeps unfolding in front of us. The airplane of tomorrow will be delivered on time.

Factors Affecting Elevator Control Forces

GEORGE S. TRIMBLE, JR.
The Glenn L. Martin Company

ABSTRACT

This paper presents an analysis and discussion of the factors affecting elevator control forces. The rate of change of elevator stick force with speed is offered as a control force criterion because: first, its value is affected by all the variables associated with longitudinal control; and second, it is easily measured on the full scale airplane in flight. Since the analysis shows a relationship between this criterion and longitudinal static stability with controls free or fixed, it is also suggested as a longitudinal static stability criterion. Additional flight testing will be required to determine reasonable limits for this criterion. Once established, it should aid the designer in his consideration of longitudinal stability and control.

INTRODUCTION

THE ELEVATOR forces required to control an airplane have become a subject of paramount importance, but the attempt to establish a criterion for them has not been very successful. Although the difficulty may arise from the lack of correlation among human reactions, it is likely that analyses of the criterion have not been sufficiently thorough. Even though a flexible criterion is needed to allow for the diversity of pilots' opinions and variations in airplane functions, its limits should be narrow because man's size and strength will remain constant. The possibility of standardizing control forces should not be overlooked for it seems but natural that the pilot's evaluation of longitudinal stability will be influenced by the control forces he is required to exert. The purpose of this paper is to present an analysis and discussion of elevator control forces.

Stability and control forces, especially in the longitudinal case, are closely related. The more statically stable the airplane is made, the greater the control forces tend to become. Free control static stability depends not only on fixed control stability, but also on control surface characteristics. As the attitude of the airplane changes, so does the angle of attack of the horizontal tail, and, in most cases, the hinge moment of the elevator. The character of such a hinge moment variation with angle of attack determines the control-free elevator position relative to the fixed surface.

The slope of the pitching moment curve of an airplane with fixed controls, as determined by wind tunnel tests, has been one of the most widely used criteria of static stability. The weakness of this parameter as a criterion is the lack of consideration for two things: one, control forces; two, free control stability. Certainly, the stick force required of the pilot must be of utmost importance in his evaluation of the stability of an airplane. The variation of stick position with speed and the rate of change of stick force with speed as measures of pilots' opinions of stability have been suggested and discussed.[1, 2] An analysis of the variation of elevator control force with speed indicates that it depends on fixed control stability and elevator hinge moment characteristics, and at the same time is proportional to free control stability. It seems obvious, then, that the variation of elevator control force with speed is a control force criterion, and probably a stability criterion.

The expression for the variation of elevator control force with speed is derived, and the relationship to free control static stability indicated. A typical airplane is assumed, and the effects of the variables discussed and plotted.

ANALYSIS

The mathematical expression for the variation of stick force with speed is written $\partial P/\partial V$, where P is stick force in pounds and V is airplane speed in miles per hour. The following equations present a method of solving for $\partial P/\partial V$ and its relation to dC_m/dC_L.

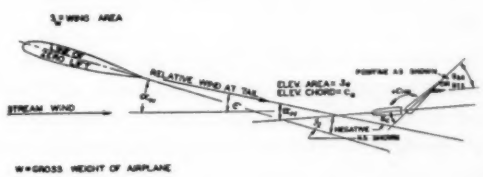


FIG. 1. Definition of symbols.

The notations used are illustrated in Fig. 1, and listed below.

δ_e	=	Elevator deflection
δ_{eT}	=	Elevator trailing angle
δ_{bt}	=	Elevator balance tab deflection
δ_{tt}	=	Elevator trim tab deflection
α_H	=	Angle of attack of horizontal tail
α_w	=	Angle of attack of wing to zero lift line
i_t	=	Incidence of horizontal tail relative to wing zero lift line
ϵ	=	Angle of downwash
S_w	=	Wing area
S_e	=	Elevator area
c_e	=	Elevator chord
W	=	Airplane gross weight
C_{H_e}	=	Elevator hinge moment coefficient

Presented at the Aerodynamics session, Ninth Annual Meeting, I.Ae.S., New York, January 31, 1941.

$C_{m_{c.g.}}$ = Pitching moment coefficient about the center of gravity
$C_{m_{C_L}}$ = Pitching moment coefficient about the center of gravity at a particular lift coefficient
C_{m_F} = Pitching moment coefficient about the center of gravity with controls free
C_L = Lift coefficient based on wing area
V = Velocity in miles per hour
ρ_0 = Air density at sea level
ρ = Air density at altitude
σ = ρ/ρ_0
F = Elevator linkage factor
K_1 = $dC_H/d\alpha$
K_2 = $\{(dC_H/d\delta_e) + [dC_H/d\delta_{bt})(d\delta_{bt}/d\delta_e)]\}$
K_3 = $(d\alpha/dC_L)_w[1 - (d\epsilon/d\alpha)]$
K_4 = $[1/(dC_m/d\delta_e)] = d\delta_e/dC_m$
K_5 = $(W/S_w)S_e c_e F = K_6K_7$
K_6 = $C_LV^2 = (391/\sigma)(W/S_w)$
K_7 = $0.00256\ F\sigma S_e c_e$
P = Elevator stick force = $K_7C_{H_e}V^2$

T subscript refers to trim speed; the speed at which control forces are zero.

$$C_{H_e} = \left(\frac{dC_H}{d\alpha}\right)\alpha_H + \left(\frac{dC_H}{d\delta_e} + \frac{dC_H}{d\delta_{bt}}\frac{d\delta_{bt}}{d\delta_e}\right)\delta_e + \frac{dC_H}{d\delta_{tt}}\delta_{tt} \quad (1)$$

$$C_{m_{c.g.}} = C_{m_{C_L}} + (dC_M/d\delta_e)\delta_e \quad (2)$$

Eq. (2) assumes elevator effectiveness is not affected by balance tabs.

Since few airplanes exhibit C_m as a straight line function of C_L, it is more practical to use $C_{m_{C_L}}$ than to substitute an equation for the C_m *vs.* C_L variation.

It is understood that the value of C_m with elevators zero for the C_L in question will be substituted in Eq. (2) for $C_{m_{C_L}}$.

For equilibrium flight, $C_{m_{c.g.}} = 0$, and the elevator deflection required for such a condition may be obtained from Eq. (2) and substituted in Eq. (1). The hinge moment coefficient for equilibrium flight conditions is then:

$$C_{H_e} = \left(\frac{dC_H}{d\alpha}\right)\alpha_H + \left\{\left[\frac{dC_H}{d\delta_e} + \left(\frac{dC_H}{d\delta_{bt}}\frac{d\delta_{bt}}{d\delta_e}\right)\right]\left(\frac{-C_{m_{C_L}}}{\frac{dC_m}{d\delta_e}}\right)\right\} + \left(\frac{dC_H}{d\delta_{tt}}\right)\delta_{tt}$$

Expanding, and substituting symbols for the various constants in the equation:

$$\alpha_H = \alpha_w + i_t - \epsilon$$

$$\alpha_H = C_L(d\alpha/dC_L)_w[1-(d\epsilon/d\alpha)] + i_t$$

the equation becomes:

$$C_{H_e} = K_1(C_LK_3 + i_t) - K_2K_4C_{m_{C_L}} + (dC_H/d\delta_{tt})\delta_{tt} \quad (3)$$

In Eq. (3), if C_{H_e} is assumed zero, an equilibrium flight condition with zero control force is created. *Such a condition is defined as trim and is noted by the subscript T.* Solving Eq. (3) for the trim tab deflection necessary for zero control force at a chosen trim C_L:

$$\delta_{tt} = [-K_1(C_{L_T}K_3 + i_t) + K_2K_4C_{m_T}]/(dC_H/d\delta_{tt})$$

Substituting this value of δ_{tt} in Eq. (3), and collecting terms, the hinge moment at any C_L when zero hinge moment occurs at C_{L_T} is;

$$C_{H_e} = K_1K_3(C_L - C_{L_T}) + K_2K_4(C_{m_T} - C_m) \quad (4)$$

$$P = K_7\{K_1K_3K_6[1 - (V^2/V_T^2)] + K_2K_4(C_{m_T} - C_m)V^2\} \quad (5)$$

In differentiating, V_T and C_{m_T} are constants;

$$\frac{\partial P}{\partial V} = 2K_7\left[\left(\frac{-K_1K_3K_6}{V_T^2} + K_2K_4(C_{m_T} - C_m)\right)V + \frac{K_2K_4K_6}{V}\left(\frac{dC_m}{dC_L}\right)_v\right] \quad (6)$$

In most cases, the designer will be interested in $\partial P/\partial V$ at trim speed $(\partial P/\partial V)_T$, in which case $C_m = C_{m_T}$,

$$V = V_T, \text{ and } (dC_m/dC_L)_v = (dC_m/dC_L)_T$$

$$\left(\frac{\partial P}{\partial V}\right)_T = \frac{-2K_6K_7}{V_T}\left[K_1K_3 - K_2K_4\left(\frac{dC_m}{dC_L}\right)_T\right]$$

$$\left(\frac{\partial P}{\partial V}\right)_T = \frac{-2K_5}{V_T}\left[K_1K_3 - K_2K_4\left(\frac{dC_m}{dC_L}\right)_T\right] \quad (7)$$

Static stability may be obtained as follows:

$$\left(\frac{dC_m}{dC_L}\right)_F = \left(\frac{dC_m}{dC_L}\right)_T + \left(\frac{dC_m}{d\delta_e}\right)\left(\frac{d\delta_{e_F}}{dC_L}\right) \quad (8)$$

assuming the elevator trails with no lag.

From Eq. (1), and assuming $C_{H_e} = 0$, the elevator trailing angle becomes

$$\delta_{e_F} = [-K_1\alpha_H - (dC_H/d\delta_{tt})\delta_{tt}]/K_2$$

Expanding and differentiating, where C_{H_e} due to trim tab is a constant and K_2 is invariable with C_L.

$$d\delta_{e_F}/dC_L = -K_1K_3/K_2$$

Substituting the value for $d\delta_{e_F}/dC_L$ in Eq. (8), there results the equation

$$(dC_m/dC_L)_F = (dC_m/dC_L)_T - (K_1K_3/K_2K_4) \quad (9)$$

Substituting the value of $(dC_m/dC_L)_T$ from Eq. (7) the expression for $(dC_m/dC_L)_F$ becomes

$$(dC_m/dC_L)_F = (\partial P/\partial V)_T(V_T/2K_2K_4K_5) \quad (10)$$

The Variables

The power for operating the elevator originates with the pilot. The maximum available is therefore a fixed quantity regardless of airplane size or performance. An upper limit to $\partial P/\partial V$ is thereby assigned, and it is the designer's responsibility to produce a satisfactory airplane in spite of this restriction. The lower limit may approach zero, but like the upper limit, cannot be conclusively defined until sufficient flight test data have been accumulated. Once determined, $\partial P/\partial V$ becomes a control force criterion. It will then be necessary to adjust the various factors influencing elevator control forces so that the allowable range of $\partial P/\partial V$ is not exceeded. The factors affecting $\partial P/\partial V$ fall into two groups; first, those whose values will be determined by other than control force criterion; and second, those with which the designer may exercise free reign for the adjustment of elevator control forces.

In the first category, the factors K_5, K_4 and K_3 predominate. (See Eq. (7).) The value of K_5 is dependent on wing loading, elevator size and linkage factor. Wing loading is a primary factor fixed by design and performance requirements and obviously remains constant for any particular design. Since the maximum movements of the control column and elevator are relatively fixed, little can be done to vary the linkage factor unless the airplane is large enough to warrant the use of booster controls. Elevator size (S_eC_e) and effectiveness (K_4) will be determined largely by the control necessary for take-off and landing. Admittedly, the control needed will depend on the static stability of the airplane, but, if efficiency in airplane design is maintained, static stability will be at a minimum and of the same order of magnitude for all airplanes. The third factor, K_3, is solely determined by the aspect ratio of the main wing. The service for which the airplane is intended imposes certain limitations on the aspect ratio and precludes the use of the factor K_3 for control force regulation. Even if K_3 were available for manipulation, its importance would prove to be relatively small, since over an aspect ratio range of 5 to 12, K_3 varies only from 6 to 8.5.

In the second group of factors, those available for design composition, the most independent ones are K_1 and K_2. (See Eq. (7).) These two factors can be adjusted without the necessity of a compromise in the rest of the airplane design. The shape of the C_m *vs.* C_L curve will be fixed by vertical center of gravity location and general airplane layout. Although the designer may increase or decrease the slope $(dC_m/dC_L)_T$ at any given trim C_L, he cannot change the relation between dC_m/dC_L at one C_L and dC_m/dC_L at another C_L.

Three factors then are available to the designer for use in producing an acceptable $\partial P/\partial V$ range, and a stable airplane. The remainder of this analysis will show the possible relationships between these factors, and an ultimate conclusion which will tend to fix the value of two of them, namely; K_1 and dC_m/dC_L.

Example and Discussion

Since so many of the factors involved in the determination of $\partial P/\partial V$ cannot be varied to adjust control forces, it is most convenient to assume a typical airplane and study the effects on that airplane of the three available design factors.

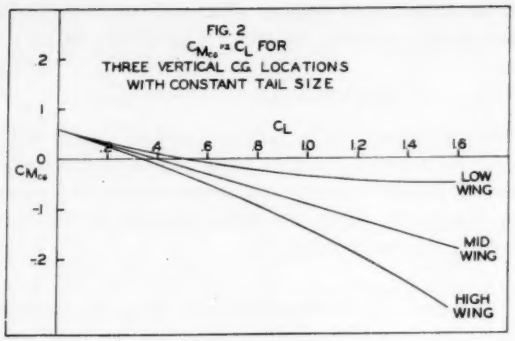
Factor	Probable Range	Assumption
Aspect ratio	5 to 12	7
W/S, wing loading	10 to 60 lbs. per sq. ft.	40 lbs. per sq. ft.
S_e, elev. area	10 to 300 sq. ft.	36 sq. ft.
c_e, elev. chord	1 to 8 ft.	2 ft.
F, linkage factor	0.1 to 1	0.3
K_1	+0.005 to −0.010	See Figs. 3, 4, 5
K_2	0 to −0.020	See Figs. 3, 4, 5
K_3	6 to 9	7.15
K_4	−30 to −100	−65
K_5		864
K_6		15,640
K_7		0.0553
dC_m/dC_L	0 to −0.3	See Fig. 2

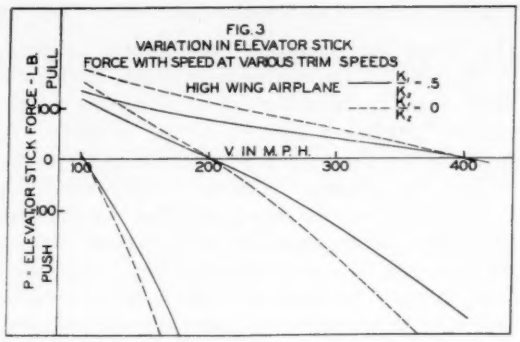
A comparison of the solid line plots of Figs. 3, 4 and 5 indicates the effect of the vertical c.g. location on the shape of the P *vs.* V curve and $\partial P/\partial V$. The moment curves of Fig. 2 and the hinge moment slopes noted on the figures were substituted in Eq. (5) to obtain these curves.

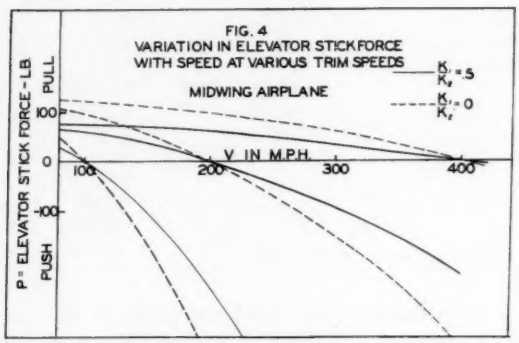
The low-wing airplane exhibits very undesirable characteristics at low trim speeds. An extended analysis shows that this airplane cannot be trimmed below 160 m.p.h. without producing unstable stick forces. The control of the high wing airplane becomes very stiff at low trim speeds. These tendencies in high and low wing airplanes have been verified in flight tests. The designer cannot change the characteristic shapes of the P *vs.* V curve so long as the vertical center of gravity location is fixed. By enlarging the horizontal tail, he could increase the fixed control static stability, $(dC_m/dC_L)_T$, which would stabilize the stick forces at slow speeds on the low wing airplane, but such a change would also increase $\partial P/\partial V$ at high trim speeds.

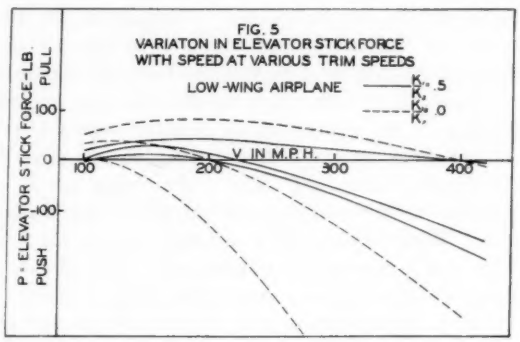
The most efficient method of obtaining a reasonable stick force slope for the low wing airplane without changing the tail size, is by reducing K_1 to zero. The resulting stick forces are plotted as dotted lines in Figs. 3, 4 and 5. The reduction of K_1 to zero increases $\partial P/\partial V$ in a negative sense, but the increase is relatively much greater for the low wing airplane than for the high wing airplane. Eq. (7) shows that $\partial P/\partial V$ is the result of the difference between two terms. Since the second term is very small for the low wing airplane at high lifts (see Fig. 2), the reduction of the first term to zero, has a very marked effect.

Another reason why the reduction of K_1 to zero is

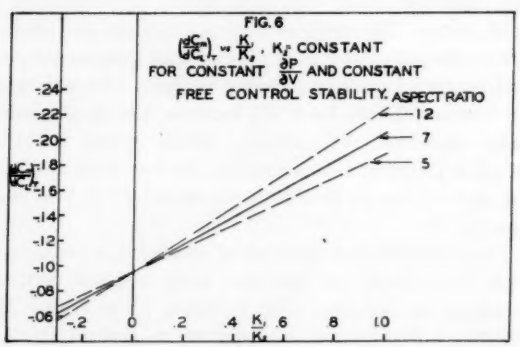








desirable may be appreciated by referring to Eq. (9), the expression for free control stability. With a fixed minimum tail size, the basic assumption, the maximum free control static stability is obtained with K_1 a *positive* finite value. There is, however, some indication that a very high positive value of K_1 might result in control surface hunting. It seems reasonable therefore to assume that the most desirable value for K_1 is zero.



To further bring out the advantage of reducing K_1, Fig. 6 is presented. The originally assumed airplane of the midwing arrangement, together with the solid line P *vs.* V curves of Fig. 4 are assumed to exemplify an airplane with minimum satisfactory stability and elevator control. In other words, the center of gravity is in the most aft position for acceptable free control stability. K_1 is then varied, holding K_2 constant. Solution of Eq. (7) for $(dC_m/dC_L)_T$ results in the required fixed control static stability with the c.g. in the most aft position. The results of this calculation are plotted in Fig. 6. If K_1 is fixed at zero, the necessary dC_m/dC_L of the complete airplane for the same free control static stability need only be one-half as great as if K_1 were equal to K_2. The resulting reduction in tail size would be in the neighborhood of 30 per cent. Of particular interest is the effect of aspect ratio at high values of K_1/K_2 as shown in Fig. 6.

To summarize this discussion; any modification of the originally assumed airplane that result in a K_1/K_2 ratio and a dC_m/dC_L that define any point on the straight line of Fig. 6, will result in airplanes having the same free control stability as defined by Eq. (9), and the same P *vs.* V curves as the solid line plots of Fig. 4. The airplanes thus evolved would be identical in the pilots' evaluation of stability.

Conclusions

In the light of the foregoing analysis and discussion, it appears that the designer is confronted with an airplane, the design of which fixes many of the factors determining elevator control forces. There remain only three available factors: K_1, K_2 and dC_m/dC_L which he may vary to provide a $\partial P/\partial V$ range suitable to the pilot. From the standpoint of design efficiency he may make K_1 zero, or possibly slightly positive. If he does, an airplane with the same static stability with controls free or fixed will result. The value of dC_m/dC_L controls free or fixed will be held at a minimum value

in keeping with flight test experience. Hence, the airplane will have the smallest possible horizontal tail. He then may vary K_2 to provide the desired $\partial P/\partial V$.

It is not the intention of the author to describe methods of producing the desired results, but it is interesting to note that the use of balance tabs, sometimes thought to reduce free control stability, has no effect on this property if K_1 is zero. A very convenient method of obtaining a value of zero for K_1 was described in reference 3.

In some cases it may not be convenient to design an elevator with a K_1 of zero. Should this condition arise, weights or springs may be utilized in the control system to effectively apply a stick force to the control column. This stick force will not vary with airplane speed. Hence the free control stability characteristics and $\partial P/\partial V$ will change as follows:

$$\Delta(\partial P/\partial V = -2P_c/V_T$$

$$\Delta(dC_m/dC_L)_F = (P_c/K_2K_4K_5)$$

where P_c is the force applied to the control column by the weights or springs. Further analysis indicates that free control and fixed control stability will be the same when

$$P_c = K_1K_3K_5$$

For an applied P_c of approximately 45 pounds, the P *vs.* V curves of Figs. 3, 4 and 5 change from the solid line plots to the dotted line plots; the same change as effected by a K_1/K_2 change from 0.5 to zero.

The determination of the correct values for $\partial P/\partial V$ and minimum allowable free control stability (equal to fixed control stability if K_1 is zero) will necessitate an appreciable amount of flight research. The measurement of elevator control forces required to maintain a given speed in flight is quite simple; so the determination of the most desirable range of $\partial P/\partial V$ should not present many problems.

The minimum allowable value of free control static stability may be more difficult to agree on since control system friction and inertia, and lag in downwash at the tail will play an important part in free control tests unless the control systems are balanced and K_1 is zero. Difficulty is to be expected in arriving at a final value for $(dC_m/dC_L)_F$ because of the consideration of the dynamic behavior of the airplane. Under any circumstances, the final choice of a design value for $(dC_m/dC_L)_F$ will remain the responsibility of the designer to a certain extent, and his choice will have to be tempered with experience and good judgment to allow for complicated variables such as the effect of power. Flight research should, however, indicate the possible range of satisfactory values assignable to this factor.

In many cases, power has a marked effect upon the static stability and control characteristics of an airplane. Further investigations of elevator control forces should include an analysis of the effects of power on free control stability and control forces.

References

[1] Gough, M. N., *Notes on Stability from the Pilot's Standpoint*, Journal of the Aeronautical Sciences, Vol. 6, No. 10, page 295, August, 1939.

[2] Koppen, O. C., *Airplane Stability and Control from a Designer's Point of View*, Journal of the Aeronautical Sciences, Vol. 7, No. 4, page 135, February, 1940.

[3] Hemphill, I. M., *Control Surface Hinge Moments and Their Relation to Stability*, presented at the Aerodynamics session, Eighth Annual Meeting, I.Ae.S., New York, January 26, 1940.

Institute Notes

Guggenheim Medal Representative Elected

Charles H. Colvin has been elected by the Daniel Guggenheim Medal Board of Award to membership on the Board, as a representative of the Institute. He is elected for a term of three years to replace **J. H. Kindelberger** whose term expires on September 30, 1941. The other Institute representatives are **Lester D. Gardner** and **J. H. Doolittle.** Mr. Colvin is a Fellow and Vice-President of the Institute.

New Corporate Members

Four new Corporate Members added during the past month have brought the number of firms holding Corporate Membership in the Institute up to a total of sixty. The companies newly affiliated are:

Chandler-Evans Corporation, South Meriden, Conn., manufacturers of aircraft carburetors, fuel pumps and fuel systems.

Guiberson Diesel Engine Company, Dallas, Texas, manufacturers of Diesel engines for tanks; also engaged in development of their A-1020 radial air-cooled Diesel engine for aircraft.

Intercontinent Aircraft Corporation, Miami, Florida, manufacturers of aircraft.

Washington Institute of Technology, Washington, D. C., consultants in radio and aircraft radio engineering.

Editor of Journal on Navy Duty

Dr. J. C. Hunsaker, Editor of the Journal and head of the Department of Mechanical Engineering and Aeronautical Engineering of the Massachusetts Institute of Technology, has been appointed by the Secretary of the Navy as Coordinator of Research and Development for the Navy.

News of Institute Members

In the recent unification of the War Department's air activities into "The Army Air Forces" unit consisting of the Headquarters Army Air Forces, the Air Force Combat Command and the Air Corps, the personnel appointed to head these divisions are all Honorary Members of the Institute; **Major General H. H. Arnold,** in addition to his duties as Deputy Chief of Staff (Air), U.S. Army, will be Chief of the Army Air Forces and its Headquarters staff. **Lieutenant General Delos C. Emmons** will be Commanding General of the Air Force Combat Command and **Major General George H. Brett** remains Chief of the Air Corps.

Announced at the same time was the creation of the Air Council in the War Department "for the purpose of periodically reviewing and properly coordinating all major aviation projects of the Army and passing on matters of current policy." It consists of the Chief of the Army Air Forces (as president), the Chief of the War Plans Division (War Department General Staff), the Commanding General of the Air Force Combat Command, the Chief of the Air Corps and, as an ex-officio member, the Assistant Secretary of War for Air, **Hon. Robert A. Lovett,** Hon. M.I.Ae.S.

General Arnold received the General William E. Mitchell Memorial Award for 1940 on June 9 at the Columbia University Club in New York. Presentation was made by Mayor F. H. La Guardia, 1939 recipient of the Award, "for the outstanding individual contribution to aviation progress" during the year.

The activities of the Research Division, Aviation Manufacturing Corporation, have been transferred to the supervision of the home office of Vultee Aircraft. **Peter Altman,** A.F.I.Ae.S., continues as Director of this section which is now known as the Manufacturing Research Department, Vultee Aircraft, Inc. Now located in Detroit, it will move to new quarters at the company's Stinson plant in Wayne, Michigan, when the building program there is completed.

F. Trubee Davison, M.I.Ae.S., President, American Museum of Natural History in New York, who was Assistant Secretary of War for Air from 1926 to 1932, has been assigned to active service in the GHQ Air Force at Bolling Field with the rank of Colonel.

E. R. Godfrey, Jr., A.F.I.Ae.S., formerly an Aeronautical Engineer with Standard Oil Development Co., has become a Sales Engineer for Pratt & Whitney Aircraft Div., United Aircraft Corp.

Howard D. Houghton, A.F.I.Ae.S., has been promoted from Assistant Manager to Manager of the Long Beach Plant, Douglas Aircraft Co.

E. E. Johnson, M.I.Ae.S., formerly Engineer in charge of the Aeronautical Equipment Division, Aeronautical and Marine Engineering Department of General Electric Co. at Schenectady, N. Y., has been made Assistant Engineer-in-charge of the department.

Albert I. Lodwick, M.I.Ae.S., has been elected a Director of Gar Wood Industries, Inc.

Lee R. Malmsten, M.I.Ae.S., formerly Chief of Ground School of Embry-Riddle School of Aviation, is now Acting Director of Embyr-Riddle's Technical Division.

The honorary degree of Doctor of Science was conferred upon **Glenn L. Martin,** Hon. F.I.Ae.S., President of The Glenn L. Martin Co., at the 173rd Commencement of Brown University. Henry Merritt Winston, President of the university, read the following citation: "Glenn Luther Martin—your life work epitomizes the history of aviation, for always you have been a leader, venturesome as a pioneer flyer, bold in military, naval and commercial design, sound and ingenious in engineering, fertile in production."

Lessiter C. Milburn, F.I.Ae.S., until recently Works Manager at the Airplane Division, St. Louis Plant, Curtiss-Wright Corp., has become Vice-Pres. & Gen. Mgr. of Bellanca Aircraft Corp.

Robert J. Minshall, F.I.Ae.S., has resigned as Vice-President and a Director of Boeing Aircraft Co. to become President of Pump Engineering and Service Corp., of Cleveland, a division of the Borg-Warner Corp.

Worcester Polytechnic Institute has conferred the honorary degree of Doctor of Engineering upon **Arthur Nutt,** F.I.Ae.S., Vice-President in charge of Engineering, Wright Aeronautical Corp.

J. B. Springfield, M.I.Ae.S., has recently returned to the staff of United Aircraft Corp., East Hartford, Conn., from the Transvaal, Union of South Africa, where he had been Service Engineer on engine maintenance and operation for South African Airways since 1938. He is now Service Representative for United.

Guy W. Vaughan, M.I.Ae.S., President of Curtiss-Wright Corp., was awarded an honorary Doctor of Engineering degree on June 7 by Stevens Institute of Technology.

Sections

San Diego. At a meeting held on June 13, attended by 373 members and guests, Ernest G. Bruce, Chief Engineer of Vultee Aircraft, Inc., presented a paper on "An Analysis of the Messerschmitt 110 Airplane."

Personnel Opportunities

The Personnel Bureau serves individual members, as well as organizations seeking to employ aeronautical specialists. Any member or organization may have requirements listed without charge.

Wanted

Stress Analysts and Layout Men. Middle western aircraft manufacturer seeks the temporary services of experienced stress analysts and layout men. Address reply to Box 136, Institute of the Aeronautical Sciences.

Public Relations Assistant. Public Relations Department of aircraft engine manufacturing company has opening for public relations assistant to gather, write and place general publicity material on company activities. Engineering degree required along with publications and/or public relations experience. Age between 25 and 35. Address reply to Box 137, Institute of the Aeronautical Sciences.

I.Ae.S. Student Branch Awards

Recipients of the I.Ae.S. Student Branch Scholastic Award and the Student Branch Lecture Award, in addition to those listed in the June and July issues of the Journal, are as follows:

Student Branch Scholastic Award: **Richard E. Wildman,** The Aeronautical University; **Edwin C. Allen,** Alabama Polytechnic Institute; **D. L. Walter,** California Institute of Technology; **J. Joanides,** Lawrence Institute of Technology; **J. A. Falvey,** Louisiana State University; **William T. Immenschuh,** Ryan School of Aeronautics; **Howard E. Roberts,** Stanford University; and **Howard Graninger,** Virginia Polytechnic Institute.

Student Branch Lecture Award: **Kenneth V. Lawson,** The Aeronautical University; **Henry Nagamatsu,** California Institute of Technology; **J. D. Stephenson,** Louisiana State University; and **Raymond Z. Nelson,** Virginia Polytechnic Institute.

Changes in Membership

The following changes in the membership of the Institute have occurred since the publication of the previous list in the Journal.

Elected to MEMBER Grade

Brugger, Richard G., B.M.E.; Assistant Project Engineer, Sperry Gyroscope Co.

Comp, LaVerne Alfred, M.S.; Asst. Professor, Mechanics Dept., University of Oklahoma.

Dysart, Joseph Justice, B.S. in Ae.E.; Aeronautical Engineer, Pan American Airways System.

Kenyon, Ralph Waldo, Engineering Flight Test Section Inspector, CAA, Curtiss-Wright Corp. (St. Louis Plant).

Mautner, Etienne, D.Sc.; Chief Engineer, Skydyne, Inc.

Elected to Industrial Member Grade

Hays, Russell, A.B.; Rotor Research.

Morton, Charles Ingalls, B.S.; Sales Executive, Export Div., Curtiss-Wright Corp.

Swailes, Earl Jack, Aircraft Materials Inspector, U.S. Navy Inspection Office, Brewster Aeronautical Corp.

Weill, Melville Kaiser, B.S. in M.E.; President, Skydyne, Inc.

Dimond, Julius Leon, Design Engineer, Aircraft Research Corp.

Elected to Technical Member Grade

Greener, Nicholas Flynn, Instructor, Defense School of Aeronautics.

Knight, Artemus Reade, A.A in Ae.E.; Production Planner (Jr.), Lockheed Aircraft Corp.

Lindblom, Charles William, S.B. in Ae.E.; Shop Liaison Engineer, Lockheed Aircraft Corp.

Myers, Oscar Bruce, Jr., B.S. in M.E.; Military Pilot, U.S. Army Air Corps.

Reynolds, Eugene Joseph, B.S. in Ae.E.; Aeronautical Engineer, Spartan Aircraft Co.

Santilli, Alcide, Chief Engineer, Feiber Instrument Corporation.

Wallender, Louis, B.S. in E.E.; Engineer, De Havilland Aircraft of Canada.

Walters, Lawrence, Jr., A.A.; Armament & Service Engineer, Harlow Aircraft Co.

Williams, Richard Samuel, Ae.E.; Technical Data Engineer, Intercontinent Aircraft Corp.

Zier, A. Avrum, Aeronautical Engineer, Brewster Aeronautical Corp.

Transferred to Associate Fellow Grade

Howard David Houghton, Manager, Long Beach Plant, Douglas Aircraft Co., Inc.

Necrology

William Ballantine Wheatley

William B. Wheatley, a Founder Member of the Institute and Chief Test Pilot for Consolidated Aircraft Corporation, was killed on June 2, 1941, when the four-engined bombing plane in which he was flying crashed in San Diego Bay.

Mr. Wheatley was born in Chester, N. Y., December 17, 1902. He graduated from the Industrial Electrical Engineering Course at Pratt Institute, Brooklyn, N. Y., in 1922. Upon completion of the U.S. Air Service Advanced Flying School training at Kelly Field, with rating as "Airplane Pilot and Airplane Observer," he held commissions as 2nd Lieutenant and 1st Lieutenant in the Connecticut National Guard Air Service until December, 1928. During the summer of 1926 he was flying instructor for Aerial Service Corp. (later Mercury Aircraft) of Hammondsport, N. Y. From September, 1926, to April, 1928, he was in the employ of Pratt & Whitney Aircraft and became their first test pilot after successively fulfilling duties as assembly mechanic, engine test house attendant and field service representative. From May, 1928, through January, 1929, he was an air mail pilot for Colonial Western Airways flying the Albany-Buffalo-Cleveland route.

In February, 1929, Mr. Wheatley became Chief Test Pilot for Consolidated Aircraft Corp., when it was located in Buffalo, N. Y. For a time he was also Service Manager for the company. He was responsible for the flight testing of all the commercial and military aircraft built by Consolidated over the past ten years—from spin testing of the first "Fleet" training planes to the testing of large flying boats and four-engined bombers.

SUGGESTIONS FOR CONTRIBUTORS TO THE JOURNAL *of the* AERONAUTICAL SCIENCES

The Institute of the Aeronautical Sciences invites both members and non-members from any country to submit papers for publication in the Journal of the Aeronautical Sciences. The Journal, following the practice of other scientific publications, does not pay for contributions.

The following directions for the preparation of papers, if followed by authors, will save correspondence, avoid the return of papers for changes, minimize the work of preparation for the printer, and save the expense due to the charges made for "author's corrections."

MANUSCRIPTS: Papers must be written in English, in original typewriting on one side only of white paper sheets, consecutively numbered, and be double or triple spaced with wide margins. Manuscripts should be prepared with great care so that they will be typographically accurate. Paragraphing should be given special attention. Papers should be written in the third person, reference to the writer being made as "the author." Avoid the use of the words "I," "we," and "you." Blueprint copies of papers are unacceptable as it is impossible to mark directions to the printer on them. Correcting, changing, or adding to papers after they are in type is costly. It is, therefore, imperative that papers submitted be in final form. Typographical errors may be corrected on proofs, but if authors wish to add material, they may do so at their own expense. In mailing, drawings may be rolled, but manuscripts should be sent flat. Send by first class mail (register if you wish for your own protection) to the Secretary, Institute of the Aeronautical Sciences, 1505 RCA Bldg. West, Rockefeller Center, New York City. All manuscripts will be examined by the Editorial Committee and by the Editor. Authors will be advised as promptly as possible (usually two to three weeks) whether the paper is acceptable for publication.

TITLES: The title of the paper should be brief. The name and initials of the author should be written as he prefers. The use of the full name of an author is advocated because of the frequent duplication of initials and surnames which sometimes makes it difficult to establish the identity of the author. This is particularly important for large annual indexing and abstracting services. All titles and degrees or honors are omitted. The name of the organization with which the author is associated should be placed after his name on the same line. The date on which the paper is received will be inserted by the Editor.

SUMMARIES OR ABSTRACTS: An abstract to be printed at the beginning should accompany each article. It should be adequate as an *index* and as a *summary*. It should contain a statement of major conclusions reached, since summaries in many cases constitute the only source of information used in compiling scientific reference indexes. Abstracts printed in other journals, especially foreign, in most cases, consist of summaries from printed papers. The summary should explain as adequately as possible the major conclusions to a non-specialist in the subject. The summary should contain from 100 to 300 words, depending on the length of the paper.

SUB-HEADINGS: Sub-headings should be inserted by the author at frequent intervals. The work of editorial preparation will be simplified by the author providing many sub-headings. Owing to the breaking of columns and the insertion of illustrations, some of the sub-headings may have to be omitted.

SHORTENING OF PAPERS: Some papers, at the end, fill in only a portion of a page. This leaves much wasted blank space as succeeding articles are started at the top of a page. Authors should indicate by notation on the left-hand side of the page what matter may be omitted when "run overs" occur. This request is important as the Journal cannot afford in the future, as it has in its earlier issues, to have blank half pages or more at the end of papers.

MATTER USUALLY DELETED: Acknowledgments of assistance in preparation of paper, except by collaborators. Photographs or illustrations of little technical interest and not showing advances in general practice. Too detailed tabular matter (general results of such tables may be included in the text). Lengthy descriptions of materials or processes or of preliminary experiments or theories which preceded final results; salient features only are of interest.

REFERENCES AND FOOTNOTES: References should appear as footnotes only, numbered consecutively, grouped together at the end of the manuscript. The arrangement should be as follows: (for books)—[1] Durand, W. F., *Aerodynamic Theory*, Vol. 1, p. 23; Julius Springer, Berlin, 1934. (For magazines)—Englund, C. R., Crawford, A. B., and Mumford, W. W., *Some Results of a Study of Ultra-Short-Wave Transmission Phenomenon*, Proc. I. R. E., Vol. 20, No. 12, pp. 481 and 482, March, 1933. Please give Author, Title, Volume, page, publisher and date of publication as indicated. Omission of one required fact causes much extra editorial work and possible inaccuracies. All references are grouped at the end of the article.

ILLUSTRATIONS: Illustrations should accompany manuscripts and each should always be referred to in the text, preferably by number. Drawings or graphs should not be larger than 12×16 inches, and must be made with jet black India Ink on white paper or tracing cloth, the latter being preferred. *Do not use typewriter for lettering.* The smallest lettering on 8×10 inch figures should be no less than $1/4$ inch high. Cross-section paper (white with black lines) may be used, but should not have more than 4 lines per inch. If finer ruled paper is used, the major division lines should be drawn in with black ink, omitting the finer divisions. In the case of finely ruled paper, only blue-lined paper can be accepted. *Tracing paper and blueprints* are not acceptable. Lettering and all markings must be large enough to be readable after reduction. Mail rolled or flat, never fold. Drawings which cannot be reproduced (including pencil drawings) will be returned to the author for redrawing, thus delaying publication of the paper. Photographs should be very distinct and show clear black and white contrasts. They must be on glossy white paper. Avoid round and oval photographs.

CAPTIONS AND LEGENDS: Legends or captions must accompany each drawing or photograph submitted. If written on the drawing or photograph, they should be placed below and well outside the part to be reproduced. It is better to place them on separate sheets of paper pasted to the back of the drawings or photographs. Each table should have a caption such as Table 1, Table 2, Table 3, etc. Captions should be complete in themselves so as to make the data intelligible to the reader without reference to the text. A duplicate list of captions for figures should be included as the last page of the manuscript. Use "Fig. 1" (not Figure 1), Figs. 3 and 4, etc., in both the text and the numbering of illustrations. In the text, "Eq. (1)," or "Eqs. (1) and (2)" are preferable to "Equation (1)." In captions and legends, except for "Fig." and "Eq." and table headings, write all words in full; do not abbreviate. Avoid placing explanatory written matter in the drawings; it should be in the text.

MATHEMATICAL WORK: Only the very simplest formulae should be typewritten; all others should be very carefully written in pen and ink, the writing to be large enough so that ample room is provided to mark mathematical matter for the printer. A considerable space for marking should be allowed above and below all equations. All complicated equations should be repeated on separate sheets with plenty of space left for marking. The solidus should be used for simple fractions appearing within the text. Make all expressions clear to the typesetter. Greek letters used in formulae should be clearly designated by name on the margin of the manuscript. All symbols should be clearly written and carefully checked. The difference between capital and lower-case letters should be clearly distinguished and care taken to avoid confusion between zero (0) and the letter (o), between the numeral (one) and the letter (ell) and the prime (′), between alpha and a, kappa and k, u and mu, v and nu, n and eta. All subscripts and exponents should be clearly marked and dots and bars over letters or mathematical expressions should be avoided. Avoid complicated exponents and subscripts. When it is necessary to repeat a complicated expression, it should be represented by some convenient symbol.

NOMENCLATURE AND ABBREVIATIONS: The National Advisory Committee for Aeronautics Nomenclature should be used in preference to any others. Standard abbreviations should be used, and it should be noted that most abbreviations are lower case, such as m.p.h., b.m.e.p., i.hp., b.hp., hp., ... etc.